The WELDING JOURNAL

FEBRUARY 1951

IN THIS ISSUE

Submerged Arc Welding with Multiple Electrodes . . 103

Flame Hardening of Large Surfaces 111

Dynamic Characteristics of D.C. Welding Machines. . 117

Tubular Sections in Machine Frame Design 139

Composite Tungsten Carbide Weld Deposits 144

Plan Not Little Things. 169

Work of the Ship Structure Committee 65-s

Stress Studies of Ship Structure Specimens 68-s

Metallurgical Aspects of Ship Steel Quality. 79-s

Residual Stresses in Steel Plates 91-s

Table of Contents. 101

Index to Advertisers 186

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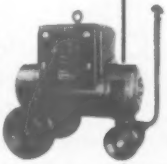

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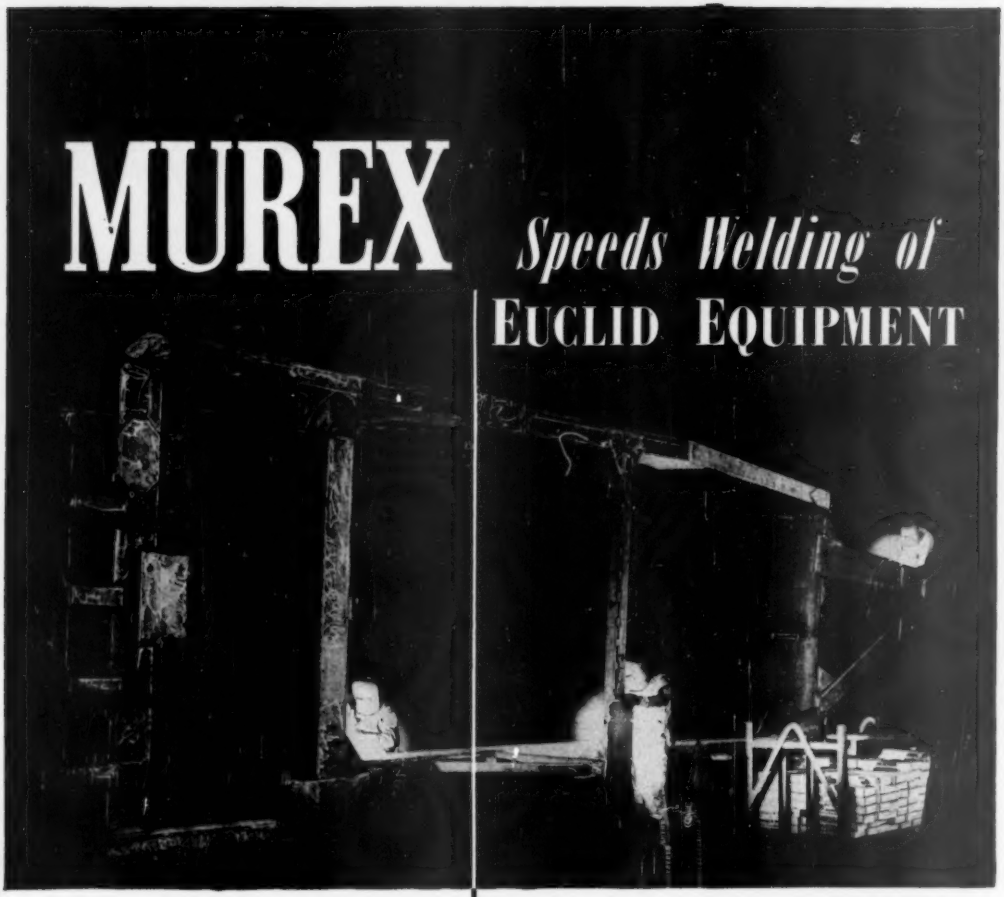

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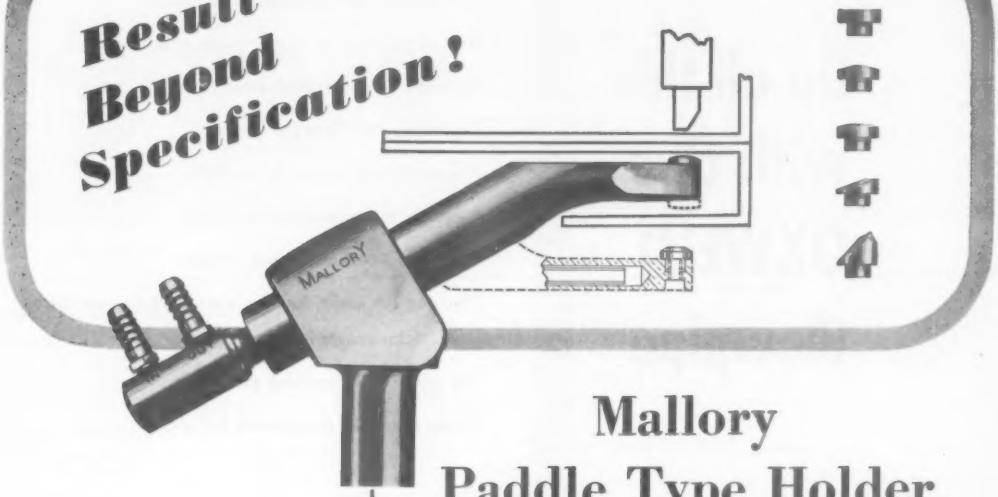

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Volume 30 **FEBRUARY 1951** *Number 2*

TECHNICAL PAPERS, ITEMS AND REPORTS

Submerged-Melt Welding with Multiple Electrodes in Series, by A. R. Lytle and E. L. Frost 103
Current Welding Literature 110
Flame Hardening of Large Surfaces, by J. J. Barry 111
1950 Adams Lecture—Dynamic Characteristics of D.-C. Welding Machines, by Charles H. Jennings 117
Tubular Sections in Frame Design of Hydraulic Benders, by E. J. DeWitt, F. J. Lammers and F. D. Alexander 139
Some Characteristics of Composite Tungsten Carbide Weld Deposits, by Howard S. Avery 144
Discussion by Gorham Woods 160
Discussion by C. E. Rogers 161
Crack Sensitivity of Aircraft Steels, Discussion by J. Koziarski 163
"Heliarc" Welding in Production, Discussion by Robert Bowman 167

PRACTICAL WELDER AND DESIGNER

Plan Not Little Things, by G. F. Nordenholt 169
Food Cabinet Production Jumps 2600% Through Use of Resistance Welding 171
Hard-Face Cultivator Sweeps for Longer Wear, by L. C. Richmann 172
Hints on How to Weld Sheet Lead, by E. J. Chester 173
Adjusting the Oxyacetylene Flame, by R. G. Harner 175
A Simple Positioner for Small Welding Jobs 176
Difficult Shipyard Jobs Speeded by Powder Cutting 177
Scrap Cutting Blowpipe Efficiently Operated on Gantry Rig 177
Some Unused Cover Pictures 178
How to Bring Worn Parts Back to Life, by E. A. Shevey 180

SOCIETY AND RELATED EVENTS

Society News 181
News of the Industry 184
Advertisers Index .. 186
New Literature 190
Personnel 198
New Products 200
Section Activities ... 204
Abstracts of Current Welding Patents . 211
List of New Members 212

WELDING RESEARCH SUPPLEMENT

Summary of the Work of the Ship Structure Committee, by Rear Admiral K. K. Cowart 65-s
Stress Studies of Welded Ship Structure Specimens, by Wm. R. Campbell 68-s
Discussion by Wendell P. Roop 76-s
Discussion by Comdr. James McIntosh, USCG and James B. Robertson, Jr. 77-s
Reports of International Welding Commissions .. 78-s
Some Metallurgical Aspects of Ship Steel Quality, by H. M. Banta, R. H. Frazier and C. H. Lorig 79-s
Discussion by James W. Halley 89-s
The Determination of Initial Stresses and Results of Tests on Steel Plates.
Part I. The General Problem and Evaluation of the Method Used, by E. W. Suppiger 91-s
Part II. The Method of the Hole, by C. Riparbelli 93-s
Part III. Experimental Techniques and Test Results, by Edward R. Ward 97-s

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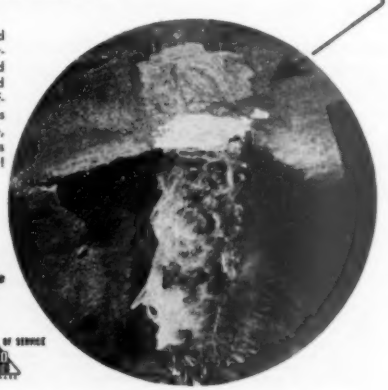

The WELDING JOURNAL

volume 30 February 1951 number 2

Submerged-Melt Welding with Multiple Electrodes in Series

A new method of submerged-melt welding is described with resultant advantages for special applications such as the cladding of steels

by A. R. Lytle and E. L. Frost

INTRODUCTION

SUBMERGED-melt welding has been widely accepted in the metal fabricating industries as an efficient tool for the production of weldments of high quality. As conventionally employed, the process consists of welding with bare rod which is fed through a suitable electrical contact assembly into the welding zone. Protection from the surrounding atmosphere is provided by a granulated welding composition or Unionmelt,* part of which is melted during the operation to submerge the tip of the rod and the molten weld puddle. With such a complete protective shield in intimate contact with the hot metal, it is possible to use heavy welding currents to produce sound welds having deep penetration into the parent metal or base plate. For example, single-pass butt welds are normally made in 1-in. thick beveled steel plate, with complete penetration through the root face, at a travel speed from 10 to 12 in. per minute.

There are certain types of welding applications where shallow penetration of the weld bead is especially desirable. Production of composite structures, such as stainless-clad assemblies for the chemical industry and similar corrosive services, are typical of such welding applications. Presently accepted techniques for producing the desired results for this type of work have involved low speeds of travel and low welding currents with relatively high arc voltages. With these modified techniques, dilution values as low as 20% may be obtained in single-layer deposits;[1] that is to say, 20% by volume of the fused metal consists of base plate, and the remaining 80% is deposited rod metal. But even this amount of intermixing with the base metal is often prohibitive, especially where it would be necessary to compensate for dilution by enriching the alloy content of the welding rod or where the presence of this amount of base-plate metal would seriously affect the properties of the deposited rod metal. Multiple-layer welding can be used to increase the alloy content in each successive layer while lessening the effect of the base metal, but such a technique greatly increases the production time and quantity of materials used, and hence the cost of the finished product.

This paper includes a description of a method whereby dilution values less than 10% have been produced consistently and some values as low as 1.5% have been attained, and describes several practical applications of the process in the metal fabricating industries. Some of the other advantages gained through this technique, such as a rapid rate of metal deposition and low-heat input to the base-plate material, are valuable for welding where penetration and dilution—usually desired when Unionmelt welding—are not of primary importance. Typical examples of production items are illustrated.

THEORETICAL CONCEPTION OF REACTIONS WITHIN THE WELDING ZONE

When one considers the basic reactions within the welding zone under normal Unionmelt welding techniques, it is immediately apparent that a large volume of parent metal is fused. At usual welding speeds the arc impinges directly onto the base plate from the extreme tip of the rod, resulting in a concentrated zone of intense heat on the plate surface. With the current-densities normally used in submerged-melt welding, the fused zone develops well below the surface of the plate. Figure 1 is a diagrammatic sketch of the welding zone as the process might be applied for surfacing. This clearly shows the location of the tip of the rod and the weld pool with respect to the surface of the parent metal.

It is common knowledge that within practical operat-

A. R. Lytle is Head of the Welding Dept. and E. L. Frost was Welding Research Engineer, Union Carbide & Carbon Research Labs., Inc., Niagara Falls, N. Y.

Presented at the Thirty-first Annual Meeting, A.W.S., Chicago, Ill., week of Oct. 22, 1950.

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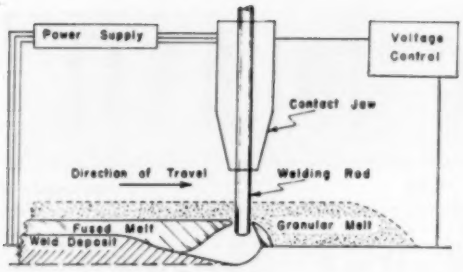

Fig. 1 Schematic diagram of submerged melt weld

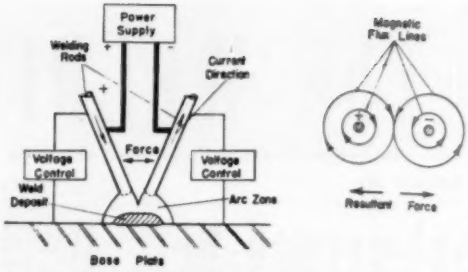

Figure 2 Schematic drawing of series-arc welding showing arc deflection within weld zone and resultant wide, flat weld bead with only slight penetration into the base plate

Right-hand figure illustrates cross section of welding rods and magnetic fields of flux surrounding each rod and the resultant repulsion of the arc streams

ing limits, weld penetration is an inverse function of arc voltage and a direct function of welding current and travel speed. For that reason present cladding and surfacing techniques involving deposits of differing alloys, where dilution should be avoided as much as possible, employ low currents (175 to 300 amp.), low travel speeds (4 to 6 in. per minute) and relatively high welding voltages (37 to 40 v.). The results of a number of tests indicate that dilution values in a single layer reach a minimum of approximately 20%.

The new method which provides a means of drastically reducing the penetrating effect of the welding arc is based on maintaining a welding action in which the welding current is completely independent electrically of the base-plate material. By appropriately controlling the location of this welding zone relative to the plate surface level, heat from the welding puddle fuses a thin layer of base material, thus forming an integral bond across the interface. The method consists of employing two or more converging welding rods connected electrically in series to an appropriate power source. Under these conditions the base plate is electrically insulated from the power source, and the welding current flows from one rod through the welding zone back through the other rod to the power source to complete the so-called "series-arc" circuit. Location of the welding zone is readily adjusted by proper positioning of the rods so that the heat input to the base plate is controlled.

Basically, the principles of series-arc welding are quite simple and follow the fundamental laws of behavior of electrical conductors. It is known, for example, that if current flows in the same direction through two parallel conductors in close proximity, the magnetic flux field surrounding these conductors will cause them to attract each other. However, if the current direction is reversed in one wire only, the magnetic forces induced by the current flow tend to repel or separate the conductors. Hence, in series-arc welding, when using either an alternating- or direct-current power supply, the instantaneous polarity and direction of current flow through each of the rods are opposite. This condition creates a repulsion between the rods and tends to deflect the arc streams outwardly, the net result being a fanning-out or spreading of the arc zone. Because the welding rods are normally positioned in a plane transverse to the direction of travel during welding, this spreading effect results in a wide, thin weld deposit. Illustrated in Fig. 2 are the flux fields and the resultant forces surrounding two electrical conductors connected in series with the power supply.

It is apparent that more than two welding rods can be introduced into this series system arranged to form an interlocking pattern of diverging weld zones to produce wider beads than obtained by a single pair of electrodes.

DESCRIPTION OF THE SERIES-ARC TECHNIQUE

A brief description of the equipment installation and the specific effect of welding variables on weld size and shape is necessary for a more complete understanding of this technique. Each rod is independently fed by its own motor and voltage control system through insulated contact jaws, any of the commonly used submerged-melt welding equipment being suitable.

Power cables are connected directly to the busbars of each welding head in such a manner that the power supply is in series electrically with the two rods. Under these conditions the base plate should be electrically insulated from the welding system and is not an essential part of the circuit, since the current path extends directly from one rod to the other through the molten pool of metal and Unionmelt. The location and positioning of the converging welding rods are regulated so that a common puddle is formed between the rods. Voltage control leads are connected between each welding head and the base plate, and the potential measured between these two points is used to regulate the rate at which the welding rods are fed into the welding zone. In this manner, different sized rods or metals of different melting points may be employed in each head, if required.

Figure 3 is a general view of a two-welding head installation employing a "Unionmelt" type DS-37 welding machine which can be used with either direct or alternating current. Notice in particular the extreme

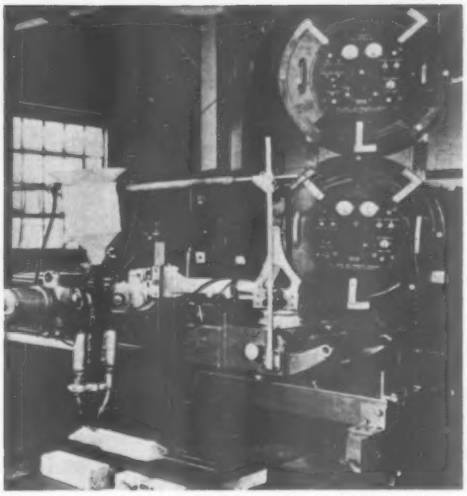

Fig. 3 Typical installation for series-arc welding showing two Unionmelt type DS-37 welding heads and voltage controls mounted on a common carriage

Note the simple jig for mounting the rod feed motors and contact jaw assemblies

flexibility of this unit and the simple jig used to interlock the contact assemblies. This equipment can be readily modified for tandem welding or disengaged for separate operation.

A close-up view of the welding zone is shown in Fig. 4. It will be noted that the rods intersect above the surface of the base plate and are normally covered by the granular welding composition shown in the background. A typical multiple-layer weld deposit is also pictured.

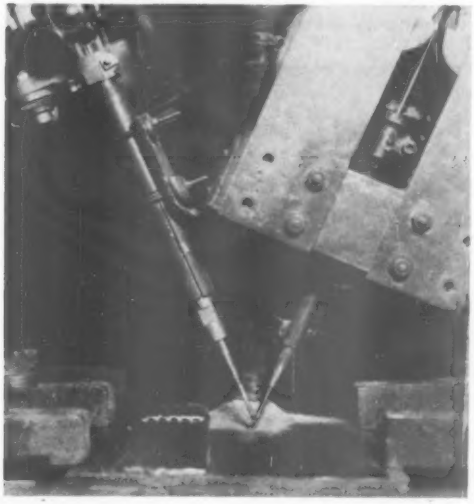

Fig. 4 Close-up view of welding zone showing location of rod intersection above the surface of the base plate and a typical multiple-layer deposit

Direction of travel was away from camera

Note the smooth overlapping of weld beads and the clean weld surface produced.

Either alternating-current or direct-current power can be used with this technique, but alternating current is preferred when welding with rods of mild steel or stainless steel. For some metals, in particular nonferrous rods, it is essential that direct-current power be used to maintain the welding operation. It has been noted for alloys where direct current is necessary that slightly deeper penetration is obtained under the positive than under the negative electrode. This condition can be minimized by offsetting the welding heads for successive beads in such a manner that the more deeply penetrating zone fuses into the edge of the previously deposited metal.

EFFECT OF WELDING VARIABLES ON WELD SIZE AND SHAPE

Of all the welding variables studied, it was definitely shown that welding current and rod positioning had the most pronounced effect on weld-metal penetration and dilution. For cladding operations where penetration should be especially shallow, low current-densities are required, and for a given rod size the welding current should be reduced to a minimum consistent with acceptable voltage stability. For example, when using $^{3}/_{16}$-in. diameter rods in each welding head, the welding current may range from 350 to 450 amp., with the intersection of the rods located from $^{1}/_{4}$ to $^{3}/_{8}$ in. above the surface of the base plate. For applications where penetration is not of primary importance, higher currents can be used.

In direct contrast to the expected results, it has been definitely demonstrated that, for this technique, relatively low voltages between each rod and the base plate are desirable to maintain low penetration and dilution. The most acceptable voltage range was found to be from 20 to 25 v., and erratic and unstable operation was observed in most cases when this potential exceeded 30 v.

The speed of travel during welding influences the position of the tips of the rods with respect to the molten pool of metal. At low travel speeds the puddle is under and in advance of the rods and produces a cushioning action on the penetrating effect of the arcs. Even under these conditions the dilution may be more than desired because of the concentration of a large amount of heat in the weld puddle. As the travel speed increases, the weld puddle becomes smaller, which decreases the tendency to melt the base metal and reduces the penetration and dilution. However, the travel speed may be increased to such a value that the puddle-to-rod position is shifted to cause direct impingement of the welding arc onto the base plate, hence producing heavier penetration. The satisfactory operating range is quite wide though, and a large amount of the present investigation was conducted at a travel speed from 10 to 15 in. per minute.

Several different rod positions were investigated, and

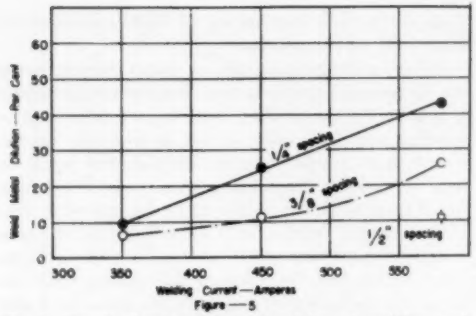

Figure — 5

Fig. 5 Graph showing effect of position of welding rods on dilution of weld metal

it was shown that the best location was in a plane transverse to the direction of travel and perpendicular to the workpiece and welding direction. The angle between rods in this plane may vary somewhat; a 45° included angle seemed best and was used in subsequent studies. Location of the rod intersection with respect to the surface of the parent metal has a pronounced effect on the penetration and dilution of the weld. As the distance of this intersection above the plate is increased, there is a gradual narrowing of the weld and a marked decrease in dilution. If the spacing is increased even further, a weld having heavily rounded edges is produced, sometimes with intermittent or no penetration into the parent metal even though the desired voltage is measured between the rods and the parent metal.

It should be emphasized that the position of rod intersection or rod spacing (as it will be called throughout this paper) does not influence the level of the welding control voltage since deposits have been made at potentials from 22 to 25 v., with rod spacing ranging from 1/8 to 7/8 in. It seems highly probable that this voltage represents the distance between the end of the rod and the surface of the molten weld bead rather than the gap distance as measured from the tips of the rods to the base plate.

The above correlations are presented graphically in Figs. 5 and 6. All these data apply to welds made with

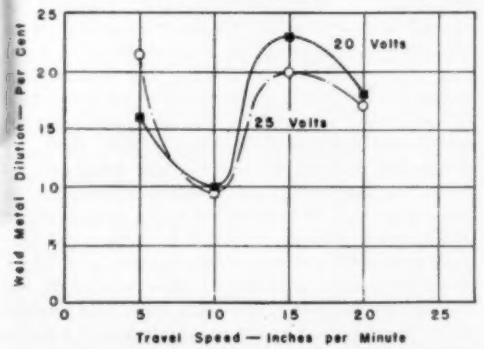

Fig. 6 Graph showing effect of speed of travel on dilution of weld metal

mild steel welding rods; some minor variations develop when welding with high-alloy or nonferrous metals because of the large differences in electrical and thermal properties of the metals. For example, copper rods may be used at a lower spacing and higher current than mild steel due to the much higher electrical and thermal conductivity of copper. Also, the characteristics of various grades of Unionmelt have minor effects on the results obtained.

As a convincing demonstration of the low dilution and penetration obtainable with this technique, single-bead deposits were produced with a stainless-steel rod on copper-backed 11-gage (1/8 in. thick) and 7/16-in. thick mild-steel base plates, and typical cross sections from these deposits are shown in Fig. 7. It is apparent that the penetration patterns are quite similar, and the maximum depth of the penetration measured over a number of sections was 0.04 in. below the surface of either plate. Under normal Unionmelt welding conditions, this would be a difficult type of deposit to make in the 11-gage steel and the penetration would be greater than 50% on the 7/16-in. plate. Therefore, considerable economic advantage is gained by the series-arc method of welding since only a small amount of energy is consumed in fusing base plate, and the large proportion of heat within the welding zone is used for melting the welding rods.

CLADDING OPERATIONS WITH STAINLESS STEELS

There are innumerable occasions where metal parts or assemblies are subject to corrosive attack and require the use of high-alloy stainless steels. However, it is frequently possible to realize a considerable saving in the production of such items if it were convenient to employ plain carbon or low-alloy steel for the main body of the structure and to use corrosion-resistant metal only

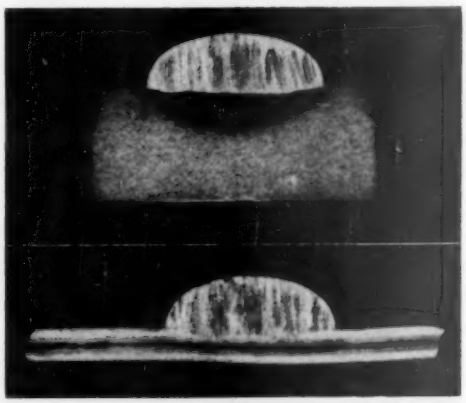

Fig. 7 Series-arc deposits produced with Type 310 welding rod on mild steel base plate using identical welding conditions

Note that the penetration patterns are quite similar in the 1/8-in. (11-gage) and 7/16-in. thick plate. 1 1/2 × magnification, etchant—HCl + H_2O_2, 350 amp. alternating current, 22 v. and 12 in. per minute travel speed

Fig. 8 Typical multiple-bead deposit produced with Type 310 welding rod

See Table 1 for chemical composition of deposit. Welding conditions the same as shown in Fig. 7. 1 × magnification, etchant—$HCl + H_2O_2$

in the zone exposed to the corrosion media. Welded fabrication of such assemblies as chemical reaction tanks and facings on large valve parts are typical of such clad structures. Since it has been demonstrated that unusually low dilution may be obtained with the series-arc welding technique, the weld metal closely approaches the chemical and physical properties of the welding rod selected for the particular application. A typical multiple bead weld cross section from a stainless-steel deposit is shown in Fig. 8.

Table 1—Chemical Composition of Series-Arc Weld Deposits—Stainless Steel

	Type	*C, %*	*Cr, %*	*Ni, %*	*Mn, %*	*Si, %*	*Cb, %*
Welding rod	309	0.084	24.30	13.68	1.88	0.47	...
Weld metal—1st layer	309	0.12	21.92	13.18	1.81	0.81	...
Weld metal—2nd layer	309	0.12	22.87	13.53	1.81	0.83	...
Welding rod	309 SCb	0.076	22.58	14.33	...	...	0.82
Weld metal—1st layer	309 SCb	0.12	19.83	12.94	...	...	0.52
Welding rod	310	0.16	25.22	20.14	...	...	...
Weld metal—1st layer	310	0.16	21.97	17.80	...	...	...
Weld metal—2nd layer	310	0.15	23.91	19.30	...	...	...
Welding rod	310	0.15	25.70	20.02	1.72	0.42	...
Weld metal—1st layer	310	0.17	21.60	18.02	1.76	0.73	...
Welding rod	304	0.056	20.75	10.15	2.14	0.18	...
Weld metal—2nd layer	304	0.086	20.10	11.93	1.88	0.59	...
Weld metal—3rd layer	304	0.074	19.54	10.63	1.97	0.56	...
Welding rod	347	0.064	19.79	11.48	2.34	0.049	1.16
Weld metal—1st layer	347	0.094	16.51	9.71	1.88	...	0.72
Welding rod	420	0.25	13.04	...	0.85	0.34	...
Weld metal—1st layer	420	0.26	11.42	...	...	...	...
Weld metal—2nd layer	420	0.27	11.90	...	...	...	...

Shown in Table 1 are data from welds using several different types of stainless-steel rods. In all cases, the base plate was $^7/_{16}$ in. thick mild steel containing 0.24% carbon. It will be noted that the total chromium loss is frequently greater than that of the other elements. This greater loss is accounted for by chemical oxidation of chromium by the fused Unionmelt, in addition to the diluting effect caused by the melted base plate. Since nickel transfers across the welding zone with substantially no loss, a fairly accurate measurement of dilution may be obtained by comparing this element in the weld deposit and in the welding rod. By this criterion, the weld in Table 1 made with Type 310 welding rod had a dilution of 9.9%. Excellent corrosion resistance was observed in all-weld-metal specimens prepared from a Type 347 deposit produced with the series-arc technique, as shown in Table 2.

Of considerable importance was the fact that fused Unionmelt removal in most of the welds described above was unusually clean, and in many cases the Unionmelt was self-detaching. This result was quite advantageous, since the only preparation required before deposition of an adjacent pass was a light cleaning with a wire brush to remove fine slivers of melt along the weld edges.

SURFACING APPLICATIONS WITH SOME HARD-FACING RODS

The same need for low dilution exists in the application of high-alloy hard-facing rods by this method. In some cases with single-electrode welding, the loss due to dilution has been offset by the use of multiple-layer weld beads to increase the alloy content in each subse-

Table 2—Composition and Corrosion Test Data for Weld Metal Specimens—Type 347 Deposit—Corrosive Medium—Boiling 65% HNO_3

Series-arc deposit	*Chemical composition*					*Corrosion rate* — *Inches penetration per month (average of two specimens)*			
						First	*Second*	*Third*	
	C, %	*Cr, %*	*Ni, %*	*Mn, %*	*Cb, %*	*48-hr. period*	*48-hr. period*	*48-hr. period*	*Average*
1st layer	0.094	16.51	9.71	1.88	0.72	0.00136*	0.00165	0.00215	0.00172
						0.00111†	0.00124	0.00162	0.00132

* As-welded.
† Annealed at 1066° C. and water quenched.

Fig. 9 Multiple-bead deposit of high-conductivity copper on mild steel base plate

Note particularly the shallow penetration pattern obtained. Chemical composition of deposit shown in Table 2—weld contained 2.55% iron. Welding conditions employed were 425 amp., direct current, 28 v., 12 in. per minute travel speed 50-grade Unionmelt, 8 by 48-mesh, preheat 700–750° F. 1 × magnification, unetched

quent layer. It has been demonstrated that when using typical welding conditions for surfacing operations, the alloying constituents continue to increase for the first four or five layers, and these values gradually level off at a point somewhat below the alloy content of the welding rod. Obviously, this fact means that there is a difference in hardness between the weld metal immediately adjacent to the base plate and at a point some distance from the parent metal.

It is interesting to note that by using the series-arc welding technique, the compositions of the first and all subsequent layers of the weld deposit closely approach that of the welding rod. For example, in Table 1, data are presented on a typical deposit produced with Type 420 rods on mild-steel base plate. Rockwell C scale hardness readings taken in the first- and second-layer deposits averaged 47.8 and were exceptionally uniform throughout the sections. Another important fact is the high carbon retention in these deposits. Since this element has a predominant influence on the hardness of the deposited metal, it is essential to retain carbon in the weld metal. Comparative results with conventional single-electrode work-in-circuit techniques show a definite loss of carbon during welding.[2,3]

Some work has been done with fabricated welding rods consisting generally of a plain carbon-steel wrapper sheath filled with finely divided high-carbon ferro-alloys. It was found that these rods operated satisfactorily with the series-arc technique and produced shallow penetration patterns similar to those obtained with solid rods. With this type of rod and technique, it has now become feasible to produce many of the high-alloy metals, frequently used for high-temperature and corrosion-resistant applications, in the form of continuous coiled rod for submerged-melt welding. Complex high-alloy metals have been quite expensive or impossible to produce in solid, coiled form for automatic welding, and dilution of these metals with base-plate material has greatly reduced their effectiveness when applied by electric arc methods. The series-arc technique offers a new and economic method for welding with these metals.

NONFERROUS CLADDING AND SURFACING APPLICATIONS

Many opportunities exist in the field of nonferrous welding and cladding where corrosion resistance, high-thermal conductivity and resistance to frictional wear are of primary importance. Again, the reduction of penetration to a minimum and the control of weld-metal composition are imperative to retain the desirable properties in the deposited metal. Copper, nickel and their many alloys are typical of metals which are useful as cladding materials on steel, and considerable development work has been done with selected alloys from these two groups.

High-conductivity copper wire was deposited as a cladding layer on mild steel, as shown in Fig. 9. It will be noted that only a thin layer of base metal has been fused in forming the bond between the weld deposit and the parent metal. For this work it was necessary to employ direct current, and the metal was deposited using 425 amp. with $^1/_4$-in. diameter rod at a travel speed of 12 in. per minute. Chemical analyses of the welding rod and deposited metal are listed in Table 3. Based on the iron content of the weld deposit, the dilution in this case was 2.46%.

Several types of copper-alloy deposits were produced with this technique, and typical composition data are listed in Table 3. Included in this group are aluminum, tin and silicon-bronzes. Welding currents ranged from 325 to 450 amp., depending upon the size of rod employed, and the welding was done at a travel speed of

Table 3—Chemical Composition of Series-Arc Deposits—Copper and Copper Alloys

	Cu, %	*Si*, %	*Al*, %	*Sn*, %	*Fe*, %	*Mn*, %	*P*, %
High conductivity copper							
Welding rod	Remainder	<0.01	...	...	0.09	0.006	...
Weld metal—1st layer	Remainder	<0.01	...	...	2.55	0.31	...
Tin bronze							
Welding rod	Remainder	...	...	2.46	0.096	...	0.13
Weld metal—1st layer	Remainder	<0.01	...	2.06	0.37	0.54	0.047
Silicon bronze							
Welding rod (nominal)	Remainder	3.40–3.85	...	...	0.25 max.	0.65–0.95	...
Weld metal—1st layer	90.28	1.80	...	...	4.35	2.47	...
Aluminum bronze							
Welding rod (nominal)	Remainder	...	8.0	...	2.75	...	...
Weld metal—1st layer	79.43	0.92	6.21	...	9.38	3.60	...

Table 4—Chemical Composition of Series-Arc Deposits—Nickel and Monel

	Fe, %	Mn, %	Si, %	C, %	Cu, %	Ni, %
Nickel						
Welding rod	0.06	0.25	0.18	0.12	0.28	98.67
Weld metal—1st layer	9.43	1.17	0.19	0.056	...	89.10
Monel						
Welding rod	0.32	0.80	0.18	0.17	28.94	67.62
Weld metal—1st layer	11.49	3.05	0.45	0.085	25.01	59.36

12 in. per minute. Because of the low-melting temperatures of these metals as compared with plain carbon steel, it was not possible to use the standard grades of Unionmelt which are designed for welding materials having melting points ranging from 1200 to 1500° C. However, welding compositions have been developed for welding these copper alloys.

Nickel and Monel deposits have been produced with the series-arc technique, and compositions of these welds are shown in Table 4. It was found necessary to reduce the speed of travel somewhat when depositing nickel to avoid porosity. The resultant weld surfaces were particularly clean after welding, and smooth overlapping of successive weld beads was obtained.

BUILD-UP AND RECLAMATION

One of the important and unique advantages of this method of welding is the attainment of a high ratio of deposited rod metal to fused base metal as compared with conventional single-electrode techniques. This result is possible because of the configuration of the welding zone. Since only a small percentage of the heat energy is consumed in fusing the base plate, the melting rate of the welding rod for a given current is considerably higher than with standard techniques. With the series-arc technique, it is possible to deposit rod metal at a rate slightly greater than twice that obtained with a single rod, with only enough base metal melted to form a complete bond across the interface. Figure 10 illustrates the rate of deposition at several current levels for various metals employed with the series-arc technique. For comparison, data are presented for the conventional single electrode, work-in-circuit technique.[4,1]

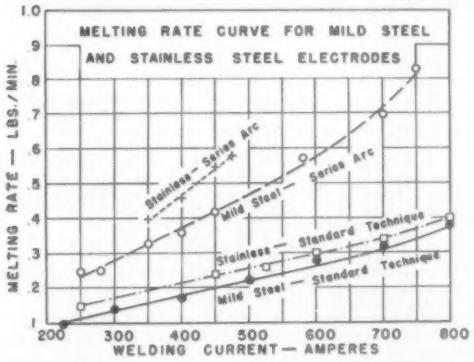

Fig. 10 Melting rate curve for mild steel and stainless steel electrodes

Figure 11 shows an example of a reinforcing strip produced at a travel speed of 18 in. per minute on $^1/_4$-in. thick steel plate. Weld dimensions were $1^1/_8$ in. wide and $^1/_4$ in. high. A similar deposit was made on $^5/_{32}$-in. thick steel plate at a travel speed of 24 in. per minute, which produced a weld pad $^3/_4$ in. wide and $^1/_8$ in. high after machining. For the former deposit, a welding current of 1200 amp. was used and for the latter case 1000 amp. alternating current was employed; in neither case was there penetration through the base plate. Copper backing bars were used to support the steel plates during welding and to confine the molten pool of metal along the outer edge.

This method of welding is economical for repairing and rebuilding worn surfaces and producing such items as integral bosses and reinforcing pads where the primary concerns are a low heat input to the base metal and a rapid deposition rate of welding rod.

Fig. 11 Transverse cross section (insert) and longitudinal view of single-bead deposit produced on $^1/_4$-in. thick base plate at 1200 amp., alternating current, 28 v., 18 in. per minute travel speed using $^3/_8$-in. diameter welding rods and 20-grade, 20 by D-mesh Unionmelt. 1 × magnification, etchant—ammonium persulphate

CONCLUSIONS

Investigation of the series-arc technique of submerged-melt welding indicates that several unique and important advantages are gained by the use of this method of welding.

1. It provides a convenient means for controlling the depth of penetration into the base metal. By use of this technique, depth of penetration is reduced to a minimum.

2. Since weld-metal dilution can be readily maintained below 10%, production of high-quality clad-steel structures by submerged-melt welding may be accomplished economically at normal speeds of travel.

3. The melting rate of the rod (weight melted per unit time per unit of current) is approximately doubled since only a small amount of heat energy within the welding zone is consumed in melting base-plate material.

4. The application of heat to the base plate can be limited, thus making possible not only the surfacing of thin base metals but also welded joints where minimum heating of the base metal is desirable.

5. For surfacing applications where high hardness is required, carbon retention in the deposited metal is much greater than in conventional single-electrode methods.

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(*Continued on page 168*)

Flame Hardening of Large Surfaces

Fundamental principles governing the adaption of flame hardening, historical development of the process, and a description of modern methods and applications

by J. J. Barry

HEAT treatment is an increasingly important operation for the engineering steels and cast irons used in our modern structures and machines, and is becoming more prevalent for members of large size which have appreciable surface areas to be protected against wear. Of all the heat treating processes, flame hardening generally requires the least expensive equipment, and at the same time offers the greatest flexibility and speed. Largely for such reasons, the process has earned an established place among modern production methods, and is particularly applicable to the large surfaces which modern requirements demand be heat treated.

The position of flame hardening is more outstanding, and more easily understood, when the elementary facts about the moving parts of modern machines are kept in mind. The bulk of the failures in machinery arises in moving parts, and usually results from wear. In examining the effects of wear on such parts, we realize that the contact surfaces—or those which wear and break down—comprise only a small proportion of the total mass of the part, often only a portion of the surface. These same parts are subject to other demands. They must stand tension, torsion, compression, impact, bending and vibration leading to fatigue, and in many cases corrosive actions as well.

Of course, we could make each part of a special material, matching the material to the specific requirements for that particular unit, and such a group of components would result in an assembly with good service life. This procedure, unfortunately, is economically out of the question today. We incline rather to the special handling of what may be termed general-purpose materials to provide a good degree of special qualities in the part. In our economy, steel is the basic material, and under proper handling, relatively few basic analyses of steel can provide most of the characteristics we require.

Fundamentally, we must guard against two causes of failure. One is the wearing of contact or bearing surfaces. The other is fracture of the body of the part, from fatigue, poor design, lack of material strength or residual stress.

Tensile properties and hardness are not the only criteria for "strength" in a *part*, as compared to strength in a *material*. The real criterion for a part is its ability to meet its service requirements. Thus, the ultimate for one part will be toughness and ductility, to meet flexing loads; for another it will be hardness suitable to meet sliding abrasion; for a third, it will be tensile strength. This is further complicated, because most parts require a combination of two or more properties in differing areas of their mass. Gear teeth should have a hard surface for wear, on a tough core and gear body for the stresses of operating load. In such a case, and in many others, the normal character of steel provides the desired quality in the mass, and we need only provide a suitable hard wearing surface to have a satisfactory part.

The job can be accomplished in several ways, and all of them are good in their proper place. Except for flame hardening and hard facing, however, they all require considerable bulky and costly equipment, operating space and appreciable time for processing. For economy, flexibility and speed, no other process can handle the variety of work with the success that flame hardening can.

When the process was first started commercially back in the 'twenties, standard 1 and 2 flame tips, similar to welding tips, were used. It was not long before the demands on the fast growing process made it necessary to develop water-cooled equipment to harden larger areas, and as one success followed another, the demand grew steadily and naturally for more elaborate equipment, capable of greater production and larger work. In recent years, machines have been developed to do automatic, mass-production flame hardening and in most major industries today, a well-equipped flame-hardening department is a familiar facility.

However, as progress continued, and the demands on the process for scope and speed grew even greater, it appeared to be heading into a capacity bottleneck. The point of insufficient gas flows to produce the required heat with existing equipment was reached years ago, and it became necessary to manifold torches to obtain enough heat to cope with the heavier sections

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and larger areas upon which we were called to work. Such measures were successful, within limits, but soon the point arrived where existing equipment could do no more—and still the demand for greater capacity continued.

The only answer was to develop completely new equipment, with very much larger flow capacities. The resulting flame-hardening torch (Fig. 1), with a flow capacity of 600 cu. ft. per hour of acetylene, has resulted in the practice of manifolding *tips* on one torch, rather than torches on one tip, and is still not the last word. It is, however, the basic tool of present large-scale flame-hardening work.

The torch is rated at 600 cu. ft. per hour flow of acetylene under ordinary operating conditions. This flow rating is conservative, and the torch can pass as much as 25% above its capacity in many practical applications where the installation is designed for small pressure losses or where higher pressures are available.

In this torch are the most modern developments in the design of oxyacetylene apparatus. The torch and mixer are combined as one unit, so no further equipment is needed for operation except a suitable burner or burners. The mixer is an advanced design, which insures practically 100% stability against flashback. The single mixer, built integral with the torch, possesses an exceedingly wide operating range, and will give the same high quality performance over the entire flow range of 0–600 cu. ft. per hour. This means that no "family" of mixer sizes is required, as in previous standard equipment. This single unit will handle any flow demand within its capacity, no matter what the application. Its efficiency of mixing is comparable to that in the highest quality modern oxyacetylene equipment.

Fig. 1 (left) Flame-hardening torch of 600 cu. ft. per hour acetylene flow capacity

Fig. 1A (right) Acetylene torch, with 600 cu. ft. per hour flow capacity, disassembled

Fig. 2 (left) Flame-hardening torch of 2250 cu. ft. per hour acetylene flow capacity

Fig. 2A (right) Disassembled view of 2250 cu. ft. per hour torch

This torch is presently designed as an air-cooled model, which is suitable for most applications of ordinary service. When used with water-cooled tips, as is almost always the case, it requires an independent source of water supply for the tip. For most installations where conditions would require water-cooling of the torch itself, because of radiant heat or flame wash, a simple water-cooled extension, long enough to remove the torch proper from the overheated zone, is all that is required. In such extreme cases as may require water cooling of the torch itself, a water-cooled model can be obtained and this type provides cooling water supply to the tip directly from the torch.

The method of operation of the torch is the same as standard heating torches, and any operator generally familiar with oxyacetylene apparatus should have no difficulty with it.

With this torch, and its far greater capacity, we have been able to simplify greatly the equipment and tip setup for many large flame-hardening jobs—replacing two or more standard torches with this one and, in many cases, noticeably improving the quality and consistency of the results as well as the economics of the operation.

However, as previously noted, this torch is not the last word. Along similar lines, a torch has been designed, built and used, which will handle a flow of 2250 cu. ft. per hour of acetylene (Fig. 2). The scope of this torch is such that we can, today, handle the largest flame-hardening work with comparative ease.

The reason for such high capacity torches is obvious when the simple formula for flame-hardening rounds is examined. It amounts to a rule of 150 cu. ft. per hour of acetylene per inch of diameter. With this formula, we can allow properly for the increasing mass of the part—but with it, we reach a requirement of 600 cu. ft. per hour on a 4-in. round, and even the huge capacity of the 2250 cu. ft. torch is exhausted at 15 in., so that two such torches are still required for a 30-in. roll. Similar, though less drastic, requirements apply in working on flat surfaces, so there is none too great capacity in these new units. They do, however, greatly simplify the problems as compared to a setup where only 250 cu. ft. per hour could be drawn through each of several torches.

The manifolding of tips (see Fig. 15), in conjunction with the large-flow-capacity torches possesses certain advantages which are none too obvious at first glance. Using segments of standard design, in multiples, the tips can be aligned to fit a wide variety of shapes and contours, without the costly design and manufacture of specially shaped units. Furthermore, the flexibility possible with segmented tips—for instance, for various sizes of rounds or various widths of flats on the same basic setup—is often highly desirable.

The comparative differences between the same setup using the older standard torches in multiple and a single large-flow-capacity unit are sketched in Figs. 3 and 4. With 250 cu. ft. per hour torches, this installation on 10-in. flat surface work required 2 torches, which re-

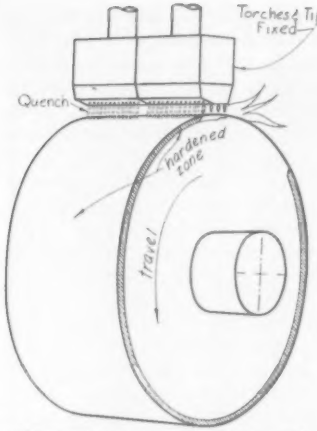


Fig. 3 Sketch of setup using two 5-in. tips of two torches to harden a 10-in. wide area

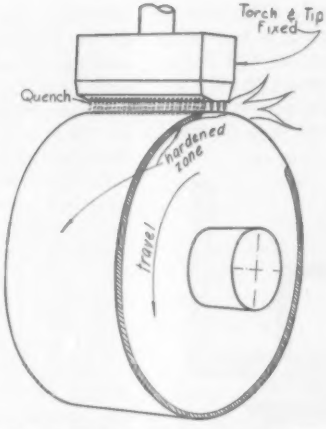


Fig. 4 Sketch of setup using one 10-in. tip and one high-flow-capacity torch for the same 10-in. wide area

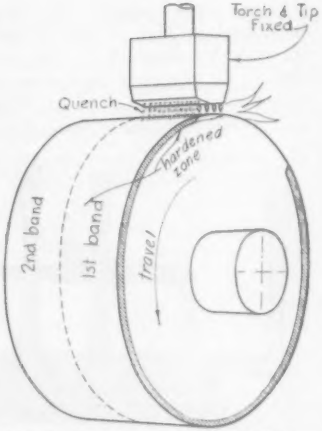


Fig. 5 An older method of hardening a 10-in. wide area uses two passes of one 5-in. tip on one torch

quired two sets of regulators, two sets of gas supply hose and correspondingly numerous connections. With a single 600 cu. ft. per hour torch, a single set of regulators and gas supply hoses, the entire setup is greatly simplified. The number of connections, regulators, hoses, etc., which are all potential trouble spots, is cut in half, for a very appreciable equipment cost saving. The control of the operation is also greatly simplified, with only one set of regulators to adjust, and with the flashback-proof, high stability characteristic of the high-flow-capacity torch.

There is a third method whereby this job could be done—and in one actual case, it was the original method for the job. This is the use of a single torch (Fig. 5) with a five-inch tip, and two passes over the work. The serious drawback in this technique is the soft band which results at the line between the two passes.

None of what has been said should be taken to mean that the usefulness of the previous standard torch is superseded entirely by the new units. The older models have performed superbly in many applications, and will continue to give good service for many new jobs, within their capacities. The high-flow-capacity units do provide, however, the means to do jobs of greater size more simply, without complicated setups and overloading of the torches, so that the flame-hardening tool can be better matched to the requirements of the job than has been the case.

In one interesting case, a leading manufacturer of earth-moving equipment had a problem with 4-in. wide shovel pads, about 18 ft. in circumference, which was met with the standard 250 cu. ft. per hour torch and a special tip. While a standard 4-in. tip would cover the width of the part in one pass, the junction at the start-stop of the circumference would present a radial soft band which the radially aligned rollers would soon wear to a low spot. By using a wider 5-in. tip and aligning it at an angle to the radius of the part (Fig. 6), this inevitable soft band was made to lie diagonally across the width of the part, and the wearing down of this area practically eliminated.

With wide flat surfaces, it is necessary to use either multiple passes, multiple tips or specially designed tips and, with these, manifolded torches, even of the high-

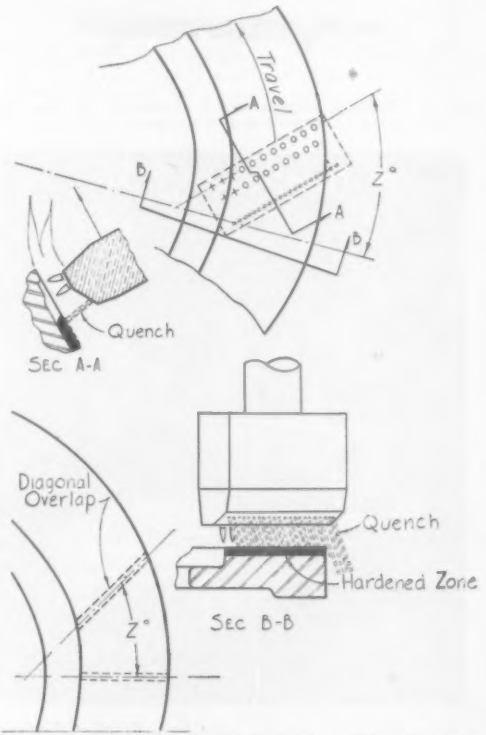
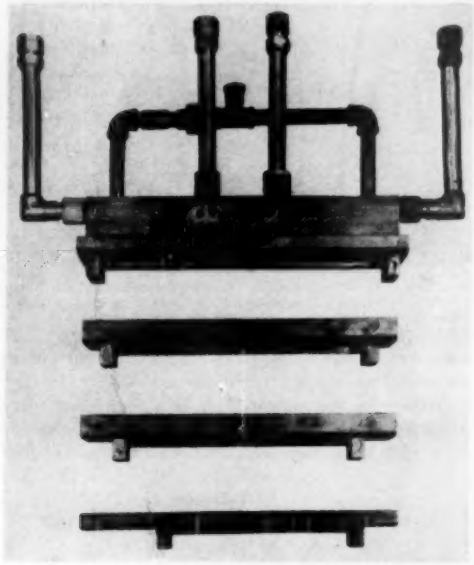


Fig. 6 Diagram of torch position and travel for hardening circular track

capacity type, usually are required. The case, illustrated in Figs. 7 and 8, hardening a part 16 in. wide by about 5 ft. long, is set up with four 250 cu. ft. per hour torches on a special tip which covers the width in one pass. With this arrangement, and an excellent pressure-drop characteristic in the gas supply system, it is possible to obtain enough heat for the job, although the torches are being worked to fullest capacity. To do the same job with large-flow capacity equipment, two 600 cu. ft. per hour torches could be used, and the equipment setup considerably improved, but the existing arrangement has proved highly satisfactory.



Courtesy, L. K. Le Blond, Cincinnati

Fig. 7 ***Multiple torches and a composite tip used to harden large flat areas***

Courtesy, L. K. Le Blond, Cincinnati

Fig. 8 ***Multiple torch-composite tip setup (Fig. 7) in use, hardening a sizable flat surface in one pass***

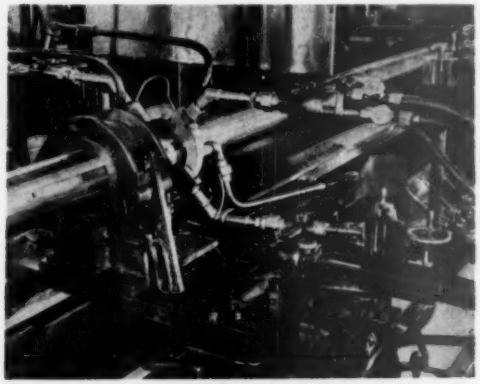
For hardening cylindrical surfaces, the progressive spinning technique is the preferred method, and is used wherever it is possible to rotate the part. With it, the maximum uniformity and freedom from soft spots is obtained, with correspondingly improved service life.

A typical application of this method to round parts up to 7 in. diameter and up to 20 ft. long is illustrated in Figs. 9 and 10. The parts are precision boring bars of SAE 4340 steel, which are finish-machined, flame-hardened, and ground. The flame-hardened bars are preferred to carburized bars for several reasons, according to the manufacturer. The 4340 steel has twice the tensile strength of the steel used for carburizing; there are a minimum of unrelieved internal stresses in flame-hardened bars, so they run true even when submitted to high cutting torques, and the flame-hardened surfaces are uniform in hardness, reducing the possibility of the boring bars galling in bushings.

The hardening of these parts is a progressive spinning

Courtesy, Giddings & Lewis Machine Tool Co., Fond du Lac, Wis.

Fig. 9 ***Lathe type setup for hardening large boring bar***



Courtesy, Giddings & Lewis Machine Tool Co., Fond du Lac, Wis.

Fig. 10 ***Close view of torches on travel carriage for longitudinal hardening of large boring bar***

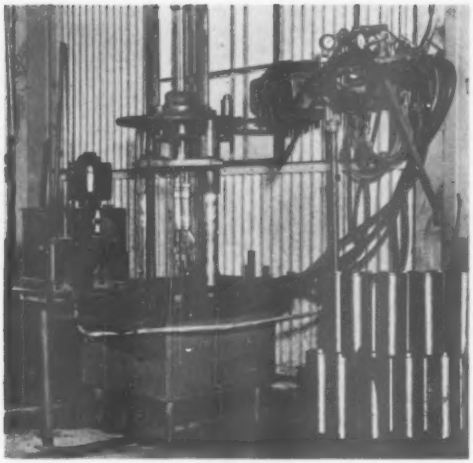


Fig. 11 Special device for hardening internal rounds

method, using four standard type torches and four separate tips arranged around the circumference of the round. Speed of revolution, traversing of the tips along the length of the work, and other factors are readily adjusted to give the exact hardness and case depth required for this precision product.

With a suitable setup, an internal cylindrical surface is as readily hardened as the external face. Figures 11 and 12 show a unit built to treat the internal surfaces of oil-well-drilling mud-pump sleeves. These are vital parts in drilling operations, and flame hardening provides the answer to a critical wear problem which used to result in costly time losses.

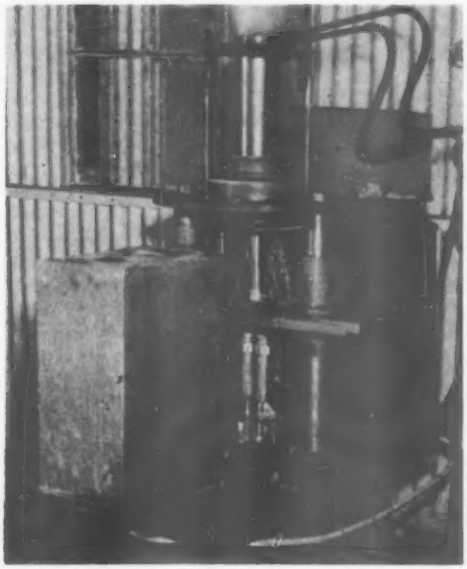


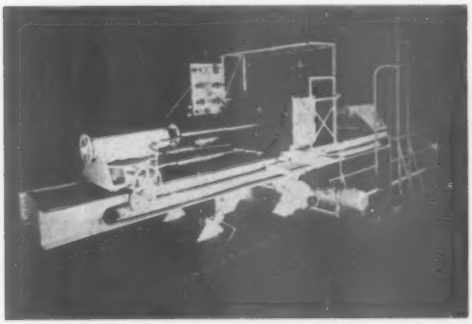
Fig. 12 Close view during operation of hardening an internal round

In this installation, two standard torches were used, each supplying one half of a two-segment matched circular tip of special design for the job. The tips are traversed upward inside the liner, which is rotated from the top of the machine, with a standard type of water quench ring, and an auxiliary compressed air quench follows upward on the outside. This auxiliary air quench is turned on after the tips have traveled 3 in., and turned off just before it reaches the top of the part, to prevent postheat buildup which would decrease the hardening results. Production on this setup is 36 liners per hour, with a consistent depth of case of 0.036 in., and distortion of only 0.003 in.—well within grinding limits. The inside surfaces test to 58–62 on the Rockwell "C" scale.

Rounds up to 8 in. can be hardened readily in the horizontal position, but above that dimension, water quench requirements make it necessary to position the part vertically. Such large diameter rounds are handled on various setups similar to that shown in Fig. 13. In these units the part is rotated, and a vertically traversing ring of torches with a quench ring built for the diameter of the part is used. Multiple torches, whether of low or high flow capacity, are required for such operations. In order to meet the requirements of such large parts, a variety of arrangements are possible, depending upon the scope for which the equipment is designed.

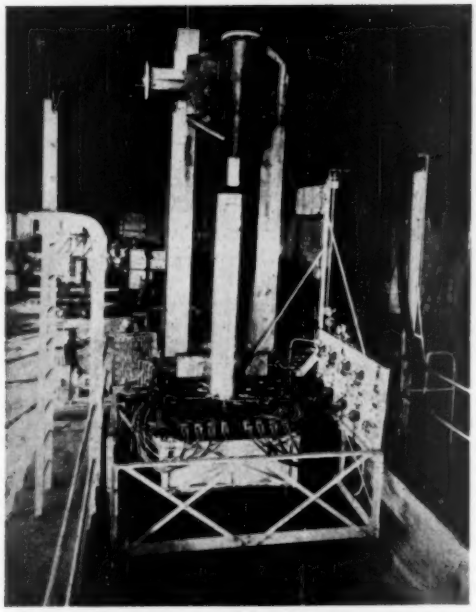
The machine illustrated in Figs. 13 and 14, for example, can carry as many as 12 sets of 250 cu. ft. per hour equipment, each of which can supply a tip up to 3 in. wide. Using this setup, rounds up to 20 in. in diameter and 20 ft. long can be hardened, with case depths running as high as $^3/_8$ in. On this particular machine, a soluble oil quench and coolant is used to prevent checking in the high-carbon materials handled.

The same unit, arranged with a high-capacity 2250 cu. ft. per hour torch installation, is sketched in Fig. 15. The comparison with the arrangement in Fig. 14 makes obvious the greater simplicity and reduction in equipment accessories with the higher capacity torches—and with two of the high capacity torches, the capacity



Courtesy, National Forge & Ordnance, Irvine, Pa.

Fig. 13 Flame-hardening machine for external rounds, over 40-ft. overall in size, lowered to horizontal position for loading and adjustments



Courtesy, National Forge & Ordnance, Irvine, Pa.

Fig. 14 Huge flame-hardening machine for external rounds in upright operating position

of the machine would be raised until it could handle rounds up to 30 in. in diameter.

Such large and massive parts as steel mill rolls, heavy shafts, compressor and piston rods, and similar sections are typical of the large diameter rounds handled on this type of equipment. Even tapered sections are hardened successfully, and other variants of the cylindrical contour can be handled with special jigging.

CONCLUSION

Flame hardening has steadily increased in importance and scope, and in recent years it has been applied to large surfaces and to parts of very great size. While much of this work has been done quite successfully by manifolding equipment of relatively small capacity, the scope of the process is greatly extended by the development of torches capable of passing as much as 2250 cu. ft. per hour of acetylene, and future prospects for even larger work than has been handled so far are excellent.

The chief difficulty in extending the scope of the process along these lines will be in the auxiliary equipment—in providing means to position and handle the parts in process and in providing adequate supplies of gas as the jobs become larger. There is nothing in either of these factors to present any real problem, and as demand warrants it, we may expect to see flame-hardening installations of really huge capacity in the future.

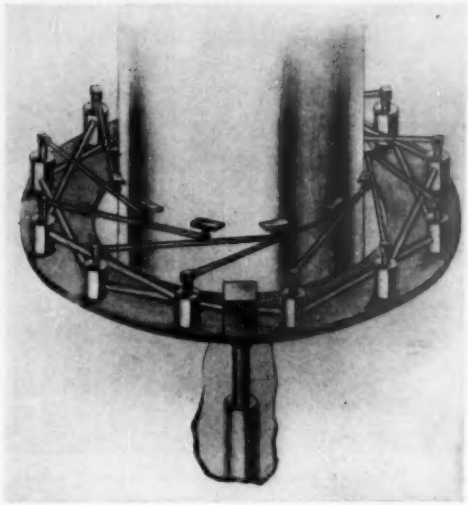


Fig. 15 Sketch of torch arrangement for high capacity torches on machine illustrated in Figs. 13 and 14

1950 Adams Lecture

Dynamic Characteristics of D.-C. Welding Machines

The electrical characteristics of a welding machine, regardless of the type of electrodes used, are a determining factor in the over-all results obtained

by Charles H. Jennings

INTRODUCTION

THERE are many types of welding processes in use today and one of the most important of these processes is arc welding. Its versatility, simplicity and general applicability to a wide variety of metals and structures has resulted in its general use in all industries. Its wide and common usage today causes many to forget its limitations 20 to 25 years ago.

The three major components of an arc-welding process are, the welding machine, either a. c. or d. c., the welding electrode, consumable or nonconsumable and the work being welded. If all three of these components are properly coordinated, good results will be obtained. In the majority of instances, the welding machine is taken for granted and the primary efforts in producing good welds are directed toward the proper selection of the electrodes, and the design and nature of the workpiece. The welding machine, however, does play an important part in obtaining good over-all results. The electrical characteristics of a welding machine, regardless of the type of electrode used are a determining factor in the over-all results obtained.

WELDING MACHINE CHARACTERISTICS

An arc-welding machine is an unusual piece of electrical equipment because in the present state of the art it is impossible to establish the required specifications and set up electrical and mechanical tests to determine whether or not it will be a good welder. The unknown factor in the specification, and the one for which there is no simple reliable test, is loosely called "welding characteristics."

By means of simple and accurate electrical and mechanical tests it is possible to determine whether or not an electric motor will do a given job. Speed, horsepower, torque, efficiency, etc. are easily determined. These items can also be determined for a welding machine but they are not a measure of its "welding characteristics," which in the past have been determined by operator acceptance.

Many investigations have been conducted on welding-machine characteristics and just as many conclusions have been drawn regarding what is desirable. All of these conclusions and theories are probably correct for certain conditions of welding although no single one is a complete answer to the problem.

Studies on five different experimental welding machines designed to have widely different electrical characteristics each of which could have been considered ideal according to the requirements established by some one theory were very discouraging. These machines were tested by 20 welding operators in such a manner that it was impossible for the operators to know which machine they were using. The results are given in Table 1.

Two possible conclusions can be drawn from a test of this type:

1. Under the conditions of the test the "welding characteristics" of the machines were not critical.
2. "Welding characteristics" are a matter of personal judgment.

The second conclusion is worthy of further comment because it has an important bearing on the value of operator tests.

During the "blindfold" test, each operator was asked

Table 1—Welding Operator Blindfold Test

Machine No.	*Times rated best*	*Times rated poorest*
1	5	4
2	4	2
3	2	6
4	4	4
5	4	2

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what welding characteristics he considered desirable. The various answers received were as follows:

1. A smooth quiet arc.
2. A crackling arc, because it appears to give better penetration.
3. Ability of a machine to hold a long arc.
4. Ability to break the arc without drawing it out too long.
5. Amount of spatter produced.

Further tests on these machines indicated that when the type of electrode was changed the operators gave individual machines entirely different ratings.

It is apparent from these studies that although operator acceptance tests are essential in determining welding characteristics they alone are not always infallible. Properly coordinated operator tests combined with theoretical analysis are essential to determine the proper electrical characteristics of an arc-welding machine. These characteristics can be divided into two general groups: static characteristics and dynamic characteristics. Each group is important, but the dynamic characteristics have the predominate influence although unfortunately they are the least understood.

STATIC CHARACTERISTICS

The static characteristics of a welding machine are commonly illustrated by means of v.-amp. curves. These curves illustrate the relationship between voltage and current from open-circuit to short-circuit conditions under steady state or static conditions. They illustrate the final relationships and give no indication of what happens before this final relationship is reached.

During arc welding the electrical circuit is periodically shorted. This condition is obtained in several ways, such as by touching the electrode to the work in order to strike the arc, by accidently shorting out the arc, and by metal from the electrode bridging the arc gap during transfer from the electrode to the work. Because of this fact it is essential that a drooping v.-amp. characteristic be used in order to limit the short-circuit current.

Two typical v.-amp. curves originating at different open-circuit voltages are illustrated in Fig. 1. Although these curves are distinctly different in slope, it is seen that they cross at some point which may be considered as the v.-amp characteristic for a given welding condition. Also it should be noted that Curve 1 which originates from the higher open-circuit voltage is the steeper curve and has the lower short-circuit current. This condition is generally true with a continuously drooping characteristic between maximum voltage at open circuit to maximum current at short circuit.

When welding, it is impossible to hold a constant arc length at all times. Varying the arc length causes the arc voltage to vary which in turn produces corresponding changes in welding current. The steeper the v.-amp. curve, within the welding range, the lower the current change is for a given voltage change. If the arc

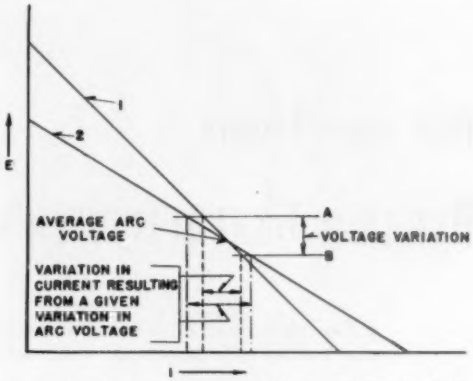


Fig. 1 Static v.-amp. curves of d.-c. welding machine

voltage changes are not too rapid the corresponding current changes can be predicted from the static v.-amp. curve.

Referring again to Fig. 1 let the horizontal lines, *A* and *B*, represent the variation in arc voltage resulting from changes in arc length. The steeper characteristic produces smaller current changes for a given arc-voltage change and a welding machine with this type of characteristic is generally known as a constant current machine. A flat characteristic produces larger current changes for a given change in arc voltage. If the slope of the curve is such that the product of current and voltage is approximately constant for a given machine setting, the machine is known as a constant energy machine.

Melting rate tests made on electrodes indicate that under normal welding conditions the melting rate is directly proportional to the current and almost independent of the arc voltage. The curves shown in Fig. 2 illustrate the effect of current and voltage on electrode melting rate.

In many welding operations it is desirable to main-

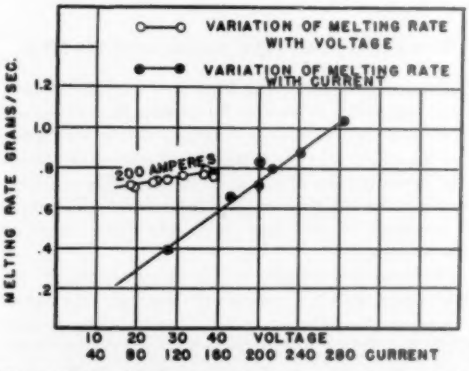


Fig. 2 Effect of current and voltage on melting rate of electrodes

tain a constant current in order to obtain maximum welding speed and quality; consequently, in such cases a steep v.-amp. characteristic is desired. In other cases, a flat v.-amp. characteristic might be desirable so that the operator can occasionally control the welding current within small limits by lengthening or shortening the arc.

The average arc voltage maintained during welding may vary between 15 and 38 v. depending upon the type and diameter of electrode used. When whipping in vertical and overhead welding or in cases of magnetic disturbances on the arc stream the arc length and arc voltage may be increased considerably above these average values. In order to maintain an arc, therefore, it is important that the available impressed voltage, neglecting transients, be higher than the voltage that might be obtained during the welding operation. The ratio between the open-circuit voltage and the maximum arc length or voltage that can be maintained is not a fixed one. It depends upon the type of power, a. c. or d. c.; the characteristics of the welding electrode; the dynamic characteristics; which will be discussed later, and the welding current. In general, however, it has been found that the available impressed voltage must exceed the arc voltage by 15 v. or more in order to maintain an arc under normal welding conditions. It is obvious, therefore, that the higher the available impressed voltage, the longer is the arc that can be maintained.

Another factor closely associated with the open-circuit voltage of a welding machine is the ease of striking an arc.

The open-circuit voltage of an arc-welding machine is not high enough to establish the arc; consequently the arc must be struck by first touching the electrode to the work and then withdrawing it. If an appreciable time is taken in this operation, the actual establishment of the arc starts from short-circuit conditions and not from open-circuit conditions.

If the base metal is covered with an insulating foreign material, the difference in open-circuit voltage as normally used on welding machines will probably have no effect unless the foreign material is extremely thin, so that its resistance will be broken down. If, however, the base metal is covered with a moderately high-resistance material such as heavy rust or scale the current flowing at the instant of contact will be proportional to the open-circuit voltage. Higher currents produce greater amounts of ionization, which in turn assist in establishing the arc.

In the majority of all cases, however, it is believed that the act of striking the arc breaks through the foreign surface impurities so that small points of clean metal are exposed.

DYNAMIC CHARACTERISTICS

It is common practice to describe the power in a welding arc in terms of voltage and current measured with suitable electrical meters. These meters are usually heavily damped and consequently give only average values. They do not produce an accurate record of the instantaneous changes in current and voltage that occur during periods of short circuit when the arc is struck or when drops transfer from the electrode to the work. Also they do not record what actually happens when a short circuit is cleared and the arc is again established.

A typical oscillogram taken during welding is shown in Fig. 3. Examination of the current trace indicates peaks of increased current and corresponding drops in arc voltage.

These discontinuities in the current and voltage traces are caused by short circuiting drops during metal transfer; the number of which may vary from zero to 40 or more per second depending upon the type of electrode used, the welding current and the arc length.

Referring to the static v.-amp. curve illustrated in Fig. 1, it is obvious that as the voltage is reduced the current should increase. This curve would indicate, however, that for a given voltage a corresponding current would always be obtained. Actually, however, the peaks in the current trace vary considerably for identical values of short-circuit voltage. These variations in current magnitude are the result of the time constant and characteristics of the welding machine and the duration of the short circuit. Time is required for the current to increase or decrease with voltage changes as can be seen by the slope of the current trace as it rises to a maximum and again reduces to the arc value. If the short circuit clears quickly, the current does not have time to build up to its maximum value. Also if the short lasts for a considerable time, the current might increase to a very high value and then drop off again to the short-circuit value. When the short circuit clears and the current again reduces, it frequently drops to a

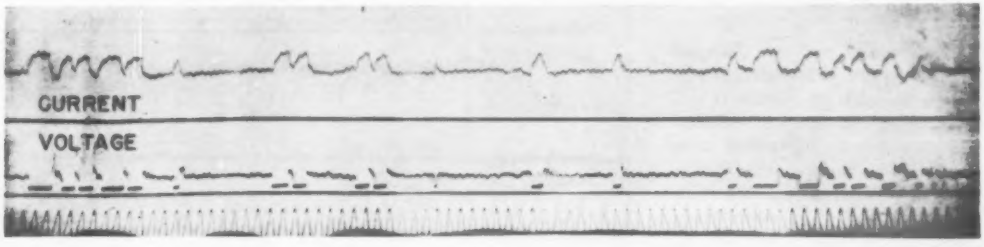


Fig. 3 Oscillogram of metal-arc welding

value below the arc current and then increases to the arcing conditions. A clearer understanding of the instantaneous relationships between voltage and current during short-circuit periods is obtained by the examination of oscillograms made using resistance loads to simulate the arc load.

To simulate transient conditions that occur when a drop of weld metal from an electrode shorts out the arc, oscillograms are taken at the instant a resistance load is shorted, thereby producing short circuit conditions. Figure 4 is a typical oscillogram of this type where the resistance load was adjusted to correspond to a welding load of 300 amp. and 40 v. Important points to note from the oscillogram are as follows:

1. The slope of the current trace or the time required to reach a maximum.
2. The current overshoot or the difference between the maximum value reached and the steady state short-circuit value.
3. The time required to reach the steady-state current condition.

The transients that occur going from the short-circuit condition to the welding condition, such as when a short circuiting drop is cleared from the electrode, can be simulated by going from short circuit to a resistance load similar to an arc load. Such an oscillogram is shown in Fig. 5. The time required to break the short circuit in this case is greater than the time required to break an actual welding short consequently the simulation is not exactly correct. It does, however, illustrate several important facts.

1. The time or rate at which the current reduces.
2. The current droop or the difference between the minimum current obtained and steady-state arc current.
3. The time required to reach a steady-state condition.

The speed of response (time required to reach steady state conditions), amount of overshoot and amount of undershoot obtained will vary greatly depending upon the welding machine, characteristics, the welding current, the welding voltage and many other conditions. All of these items have an important influence on welding characteristics, that is, the ability to hold a short arc and weld in special positions, the ability to weld with electrodes at low-current densities and the ability to maintain a steady arc without current fluctuations.

INFLUENCE OF V.-AMP. CURVE SLOPE ON WELDING CHARACTERISTIC

The slope* of the v.-amp. curve of a welding machine is frequently considered to be a measure of the welding characteristics, particularly from the standpoint of maintaining a short arc, an essential factor in deep groove; vertical and overhead welding when deep penetration is desired.

Preliminary investigations disclosed that welding

* Slope is the ratio of the change in current corresponding to a small change in voltage. The steeper the v.-amp. curve, the greater the slope.

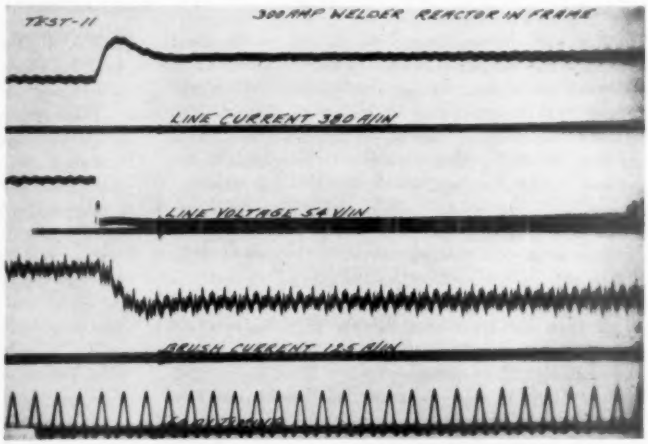


Fig. 4 Oscillogram of resistance load to short circuit

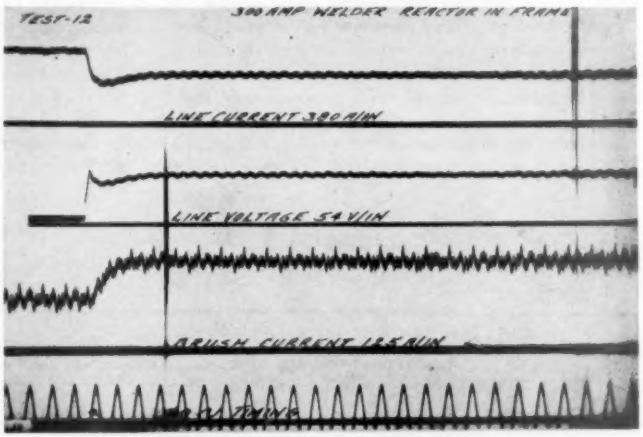


Fig. 5 Oscillogram of short circuit to resistance load

Table 2—Open-Circuit Voltage and Short-Circuit Current Data

Test No.	Open-circuit voltage	Arc-data current	Voltage	Short-circuit current	Welding machine
1	20	195	12	600	C.P. No. 17
2	30	210	11	325	C.P. No. 17
3	42	200	12	280	C.P. No. 17
4	55	210	14	280	C.P. No. 17
5	60	200	11	240	S.O. No. 1
6	96	200	15	230	C.P. No. 2
7	133	200	16	220	S.O. No. 2
8	150	210	15	230	C.P. No. 2
9	264	195	15	210	C.P. No. 2

C.P. refers to constant potential welding machine.
S.O. refers to single operator welder.

machines which made the maintenance of an arc in deep grooves easy, also made it possible to hold a shorter arc with automatic welding equipment when depositing a bead on a flat plate. This fact made possible a convenient method of measuring the minimum stable arc voltage that could be maintained by recording the arc voltage on a graphic voltmeter.

Two constant potential welding generators and two single-operator welding machines in combination with an automatic welding unit were used to study the effect of v.-amp. curve slope on the ability to hold a short arc.

Table 2 gives open-circuit voltage and short-circuit current data for the different machine settings tested.

Tests were conducted by depositing string beads on $^3/_8$-in. thick mild steel plate using $^3/_{16}$-in. diameter Grade E 4510 electrodes fed by an automatic arc-welding machine. An Esterline-Angus recording voltmeter was used to graphically record the arc voltage. The welding speed was held constant at 10 in. per minute in all cases.

The welding generator in each case was set to develop approximately 200 amp. at 17 v., the voltage at which each test was started. As the test progressed, the arc voltage was reduced in a series of steps by adjusting the voltage control of the automatic welding machine until the welding condition became unstable. The condition of instability is readily determined by a change from a steady to a wide variation in the voltage trace as shown in Fig. 6.

The minimum steady arc voltage obtained for each value of open-circuit voltage is shown in Table 3.

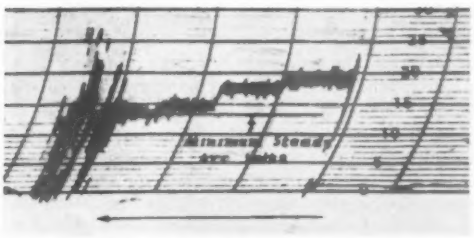


Fig. 6 Minimum arc voltage trace with No. 2 generator 80 v. open circuit

Table 3—Minimum Stable Arc Voltage

Test No.	Open-circuit voltage	Minimum steady arc voltage	Welding machine
1	20	9.7	C.P. No. 17
2	30	11.7	C.P. No. 17
3	42	13.0	C.P. No. 17
4	55	13.4	C.P. No. 17
5	60	11.0	S.O. No. 1
6	96	14.0	C.P. No. 2
7	133	13.0	S.O. No. 2
8	150	14.4	C.P. No. 2
9	264	14.7	C.P. No. 2

The curve in Fig. 7 shows the minimum stable arc voltage plotted against the open-circuit voltages at which they were obtained.

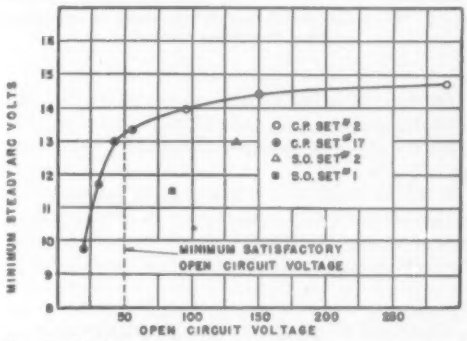


Fig. 7 Minimum steady arc voltage vs. open-circuit voltage

Little change in minimum arc voltage occurs between 50 and 264 open-circuit v. on the C.P. sets. Below 50 v. open circuit the minimum steady arc voltage rapidly decreases, indicating that the high short-circuit currents which would result from these extremely flat characteristics may have some effect. Below 50 v. open-circuit voltage, however, it became more difficult to keep the arc from breaking due to normal variations in arc length. It will also be noted that the minimum steady arc voltages obtained with the two single-operator machines do not fall on the curve produced by the two constant potential machines.

Table 4—Minimum Steady Arc Voltages for Various Short-Circuit and Surge-Current Ratios

Test No.	Welding machine	Minimum arc v.	Short-circuit I Welding I	Surge I (max.) Welding I
1	X-1000-13	9.5	5.00	2.35
2	No. 17-20 V.O.C.	10.0	3.00	...
3	S.O. No. 1-30 V.O.C.	11.0	1.20	2.15
4	No. 17-30 V.O.C.	11.0	2.00	...
5	S.O. No. 1-100 V.O.C.	12.5	1.15	1.80
6	X-400-16	12.5	2.00	1.90
7	X-600-7	12.5	3.00	1.60
8	S.O. No. 2	13.0	1.10	1.50
9	X-600-4	13.0	3.00	1.35
10	No. 2-60 V.O.C.	13.5	1.15	1.15

Tests made with $^3/_{16}$-in. sulcoat electrodes welding at 200 amp.

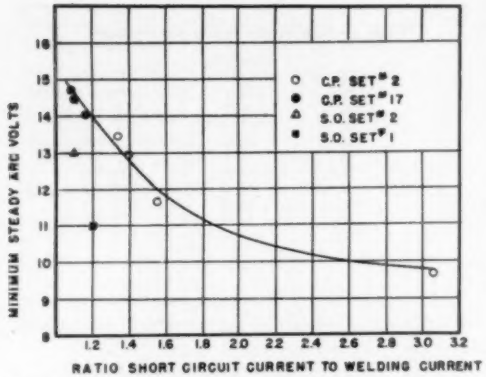


Fig. 8 Minimum steady arc v. vs. ratio short-circuit current to welding current

The ratio of short-circuit current to welding current for each machine setting used in these tests is listed in Table 4 and plotted against minimum steady arc voltage in Fig. 8.

It will be noted from Fig. 8 that the minimum arc voltage decreases as the short circuit to welding current ratio increases. It will also be noted that the data from the single operator sets again fall off the curve.

TRANSIENT SURGE CHARACTERISTICS

Overshoot in current as illustrated in Fig. 4 is a transient characteristic which may be described as follows:

If the resistance of the load on a generator is suddenly decreased, the current may increase not only to the steady value as defined by the static v.-amp. characteristic, but may momentarily rise above the final static value by an amount commonly known as the overshoot in current. The amount of overshoot is commonly given by expressing the maximum transient current in per cent of the final static current, see Fig. 4.

Oscillograms of this type were taken on a number of generators with varying transient characteristics for which the relative ability of the generator to maintain a short, steady arc was known. It was observed that

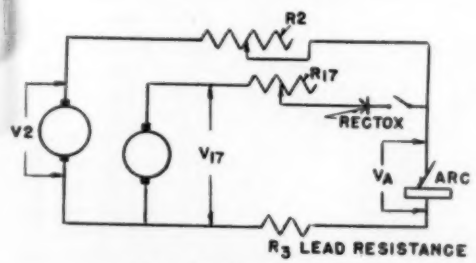


Fig. 9 Experimental circuit

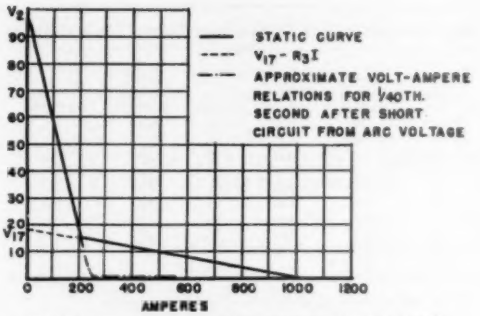


Fig. 10 V.-amp. curve of experimental (X-15-1000)

shorter arcs could be held with generators where oscillograms exhibited the greater overshoot currents. It was apparent, however, that there was no great consistency in the results.

With most welding generators the transient characteristics can be varied but only with an accompanying variation in the static characteristics. In order to overcome this difficulty, a special circuit, as shown in Fig. 9, was devised from which a static curve such as shown in Fig. 10 was obtained. The steep part of this curve could be maintained constant while the flat lower part could be varied.

The arrangement and operation of the circuit is as follows:

The constant potential generator No. 2 operated at an open-circuit voltage of 100 v. and with its ballast resistors, *R2*, connected in the usual manner. It supplied the steep v.-amp. characteristic in the upper part of the curve and in the extended dotted portion at low arc voltages. The No. 17 generator with like polarity is connected in parallel with No. 2 generator through a common ground lead and at the arc terminal through a single-way rectox unit. No. 17 generator is operated at low open-circuit voltage and the rectox unit is so connected that when the arc voltage is lower than the open-circuit voltage of No. 17 generator it supplies current to the arc in addition to that delivered by No. 2 generator. At high arc voltage, however, an inverse voltage exists across the rectifier unit and the tendency of part of the current delivered by No. 2 generator to flow through No. 17 generator in opposition to its terminal voltage is effectively blocked. In effect at high arc voltage, No. 17 generator is isolated from the arc circuit; while at low voltages it supplies additional current, the amount depending on the voltage of No. 17 generator and the resistance, *R17*, in the circuit.

Electrode metal is transferred to the weld through the arc, principally as a series of drops large enough to bridge the arc gap between the electrode and work and momentarily cause a short circuit. It is believed that if these metallic bridges are maintained for a sufficiently long period of time, they will solidify and freeze the electrode to the work and interrupt the welding process. It is further believed that high short-circuit currents or overshoots are important in limiting the duration of the

short circuits either by the "pinch effect"* or by melting away this metallic connection thereby preventing interruption of the welding process.

The experimental circuit shown in Fig. 9 was used to verify this theory.

The experimental circuit was operated in the following manner:

Where possible the open-circuit voltage of generator No. 17 was held below the average arc voltage so that it supplied current only during short circuits. The short-circuit current was varied over a wide range by varying the resistance and open-circuit voltage of this generator. Results similar to those shown in Fig. 8 were obtained. With higher short-circuit currents, the minimum steady arc voltage could be reduced but inordinately high static values were necessary to produce the desired results; values comparable with those obtained from a self-regulating welding generator with a high overshoot current. Approximately 800 amp. short-circuit current, for instance, was necessary to produce results equivalent to those obtained with a self-regulating generator whose maximum current following a short circuit from 20 v. at 200 amp. reached 500 amp.

A mirror oscillograph was also used to record current variations while welding with the various power sources. An oscillogram for the experimental circuit is shown in Fig. 11. The maximum value which the current reached during a drop transfer of maximum duration is referred to as the surge current to differentiate it from the overshoot of self-regulating generators and the static short-circuit current of constant potential power sources.

The maximum duration of a short circuit which appeared on the oscillograms while welding with a steady arc was $^1/_{60}$th second. The majority were cleared in $^1/_{60}$th second or less. For purposes of comparison, the surge currents were taken as the value to which the current rose $^1/_{60}$th second after a short circuit from average arc voltage and average welding current. Where oscillograms similar to that shown in Fig. 4 were available, surge measurements were taken from them. Otherwise, measurements were obtained from oscillograms made while welding.

* "Pinch Effect" is the contraction produced in a liquid conductor by the magnetic forces resulting from the current carried by the conductor itself. The forces are proportional to the square of the current.

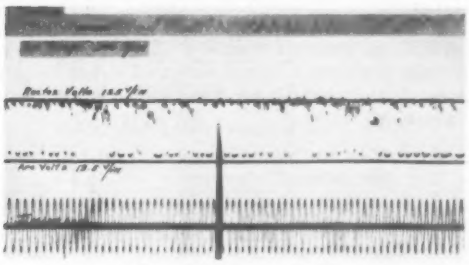


Fig. 11 Oscillogram welding with experimental circuit

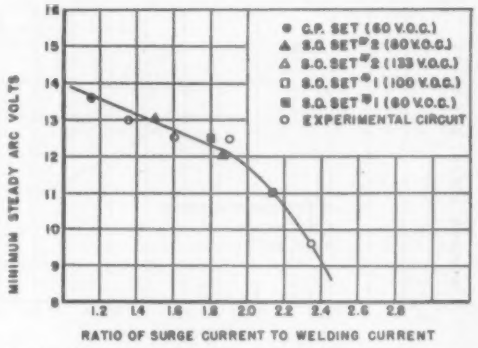

MINIMUM STEADY ARC VOLTS
RATIO OF SURGE CURRENT TO WELDING CURRENT
C.P. SET (60 V.O.C.)
S.O. SET #2 (60 V.O.C.)
S.O. SET #2 (133 V.O.C.)
S.O. SET #1 (100 V.O.C.)
S.O. SET #1 (60 V.O.C.)
EXPERIMENTAL CIRCUIT

Fig. 12 Minimum steady arc v. vs. ratio-surge current to welding current

Surge currents of the different sources of welding are listed in Table 4 as a ratio of the surge current to average welding current. Corresponding minimum steady arc voltages as obtained with the automatic machine are also listed.

The experimental rectox circuit shown in Fig. 9 is listed in the table several times, the lower part of the static characteristic being different in each case. This circuit is identified in the table by an "*X*" followed by two numbers locating pertinent points on the static curve. The first number after the "*X*" indicates the static short-circuit current. The second number gives the highest voltage across a resistance load at which No. 17 generator will supply current to the load; i.e., the voltage at which the slope of the characteristic of the entire circuit sharply decreases.

The surge ratios listed in Table 4 are plotted against minimum steady arc voltages in Fig. 12. In spite of the wide variations in type and characteristics of the source of current, the points all fall close to a smooth curve. This curve indicates that the minimum steady arc voltage which may be maintained, decreases slowly at first as the surge ratio increases, then drops more rapidly. The ratio should approach two to provide a pronounced lowering of the possible minimum steady voltage. The lowest minimum arc voltage, 9.5 v., was

Table 5—Variation of Minimum Steady Arc Voltage with Static Short-Circuit Current

Test No.	*Minimum arc v.*	*Short-circuit current*
1	11.0	1000
2	11.7	800
3	13	600
4	14	400
5	15	300
6	15	230
7*	12	240
8†	13	220

NOTE: Generator No. 17 set to become active at 11 v. short-circuit current. Generator No. 2 set at 200 amp. at 14 v. (100 v. open circuit).

* Self-Regulating single-operator machine No. 1.

† Self-Regulating single-operator machine No. 2.

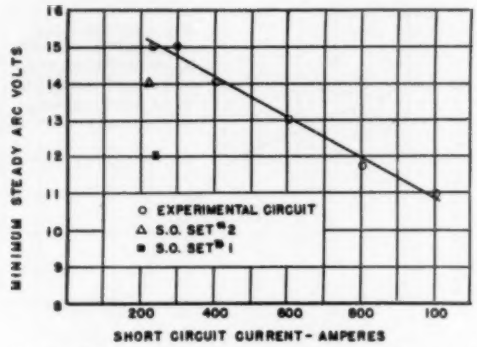


Fig. 13 Minimum steady arc v. vs. short-circuit current

obtained with the experimental circuit which developed a ratio of 2.35. Considerable splatter of weld metal was observed during the tests however.

Further evidence that only the current reached during the short circuit caused by the drop transfer is effective, was obtained in the following way. The experimental circuit, Fig. 9 was used, the voltage of generator No. 17 adjusted to supply current to a resistance load at 11 v. or below, and the resistance in series with No. 17 generator varied to modify the static short-circuit current. The minimum steady arc voltage while welding was obtained, as described before, from the graphic voltmeter and listed against the corresponding static short-circuit current in Table 5. These values are plotted in Fig. 13. The values for the self-regulating single-operator type machines again fall far off the curve.

The effect of the surge added in the experimental circuit by No. 17 generator is clearly shown in the graphic voltmeter chart in Fig. 14. The switch connecting generator No. 17 and the rectox unit to the circuit was left open and generator No. 2 was operated as a simple constant potential generator.

The graphic voltmeter traced the arc voltage as it was gradually reduced until the arc became unsteady as indicated by the wide variation in the trace at about 14 v. The switch was then closed, allowing surge currents from No. 17 generator to flow during short circuits.

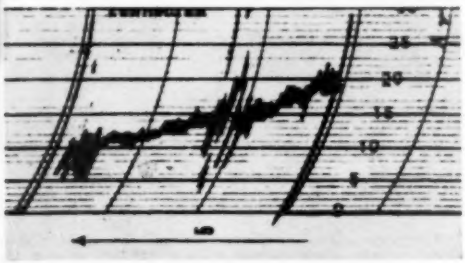


Fig. 14 Minimum arc voltage trace with experimental circuit

The arc immediately became steady and the arc voltage could be further reduced to about 10 v. before again becoming unsteady.

The results obtained from the studies made with the automatic welding machine were checked by observing the arc action during manual welding in deep grooves in the downhand, vertical and overhead positions of welding.

The same amperage was used in all cases and both bare and coated electrodes were tested. In every case it was found to be easier to hold a short arc with the experimental circuit producing a high surge current.

Direct comparison of the effect of surge was easily made during manual welding by using the experimental circuit both with and without the No. 17 generator connected. The greatest effect of surge was observed with bare type electrodes. With coated electrodes, the presence or absence of surge in the welding circuit made less difference. With E6010 type electrodes it was found to be somewhat easier to hold a short arc in a deep groove without having the electrode freeze when surge was present. With E6020 type electrode no difference could be noticed because the cupping effect of the flux coating made it impossible to hold an arc short enough to cause the electrode to stick.

Investigations were also conducted with nonferrous electrodes, such as inconel, monel, bronze, etc. The metal transfer from these electrodes is of a globular nature. The molten metal collects in large balls on the electrode end, then suddenly drops off. The presence of surge in the welding current allowed a shorter arc to be held, consequently more frequent drop transfers of shorter duration occurred and a somewhat steadier arc resulted.

WELD QUALITY AND DEPOSITION TESTS

During the automatic and manual welding tests conducted to investigate the importance of surge on welding characteristics, it appeared that the high surge currents influenced the quality of the weld metal and the electrode deposition efficiency.

To investigate this condition, comprehensive weld tests were made by making butt welds on $^{3}/_{4}$-in. plates beveled to 60° included angle and with $^{1}/_{8}$-in. root spacing. Grade E6010 and E4510 electrodes were used. Flat position welds were made with $^{3}/_{16}$-in. diameter electrodes and vertical and overhead welds were made with $^{5}/_{32}$-in. diameter electrodes. Three conditions of voltage and current were investigated and the experimental circuit shown in Fig. 9 was used to furnish power in each case.

A—Normal arc voltage without surge present (No. 17 disconnected).

B—Normal arc voltage as in A but with surge (No. 17 connected as in Fig. 9).

C—Minimum steady arc voltage with surge.

The voltage on No. 17 generator was set at 12 v. and

Table 6—Effect of Surge on Deposition

A...No surge; normal arc v.
B...200% surge; normal arc v
C...200% surge; low arc v.

F...Flat Position—$^{5}/_{16}$-in. rod
V...Vertical Position—$^{5}/_{32}$-in. rod
O...Overhead Position—$^{5}/_{32}$-in. rod

Piece No.	No. of passes	Arc v.	Arc current	Electrode Used, gm.	Electrode Deposited, gm.	Welding time, min.	Deposition efficiency, %	Deposition rate, oz./min.	Corrected* deposition rate, oz./min.	Corrected* melting rate, oz./min.
					E4510 electrode					
1-A-F	13	14.4	163	1453	1335	64.2	92.0	0.733	0.765	0.831
1-B-F	13	14.3	170	1394	1235	62.5	89.0	0.696	0.696	0.782
1-C-F	13	12.0	174	1398	1265	66.3	90.5	0.671	0.655	0.724
1-A-V	6	14.0	163	1400	1305	62.6	93.0	0.735	0.767	0.824
1-B-V	6	13.6	173	1377	1265	66.5	92.0	0.668	0.656	0.714
1-C-V	6	12.2	168	1424	1320	72.0	93.0	0.646	0.654	0.704
1-A-O	15	14.5	168	1487	1320	81.8	89.0	0.568	0.574	0.645
1-B-O	14	13.8	170	1399	1235	72.8	88.5	0.598	0.598	0.677
1-C-O	14	13.3	164	1452	1305	78.2	90.0	0.588	0.610	0.677
					E6010 electrode					
2-A-F	10	19.5	160	1366	1135	40.3	83.0	0.993	0.993	1.195
2-B-F	10	19.6	157	1460	1220	42.2	83.5	1.020	1.040	1.245
2-C-F	10	19.3	157	1495	1290	41.8	86.5	1.088	1.108	1.280
2-A-V	6	19.3	124	1592	1248	63.2	78.5	0.695	0.728	0.928
2-B-V	6	19.3	124	1594	1275	62.0	80.0	0.725	0.760	0.950
2-C-V	6	19.1	128	1570	1290	58.4	82.0	0.778	0.789	0.962
2-A-O	9	20.3	133	1778	1290	72.4	72.5	0.629	0.614	0.848
2-B-O	9	19.7	128	1832	1290	70.8	70.5	0.643	0.653	0.927
2-C-O	9	19.8	126	1764	1305	68.9	74.0	0.668	0.690	0.904

* E4510 electrodes corrected to 170 amp.; E6010 electrodes—downhand welds—corrected to 160 amp.; E6010 electrodes—vertical and overhead welds—corrected to 130 amp.

the resistance in the circuits of both generators were adjusted to give a welding current as listed in Table 6 with a short-circuit current of 550 amp., giving a surge ratio of two or greater. All testpieces were made by manual welding.

The results of the deposition tests are listed in Table 6 and shown graphically in Figs. 15 and 16. The accuracy is within 2% for the weight measurements and within $^{1}/_{2}$ of 1% for the time measurements.

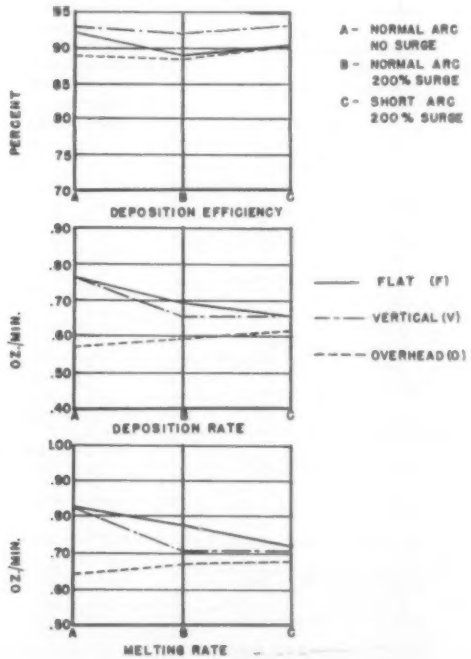


Fig. 15 E4510 electrodes

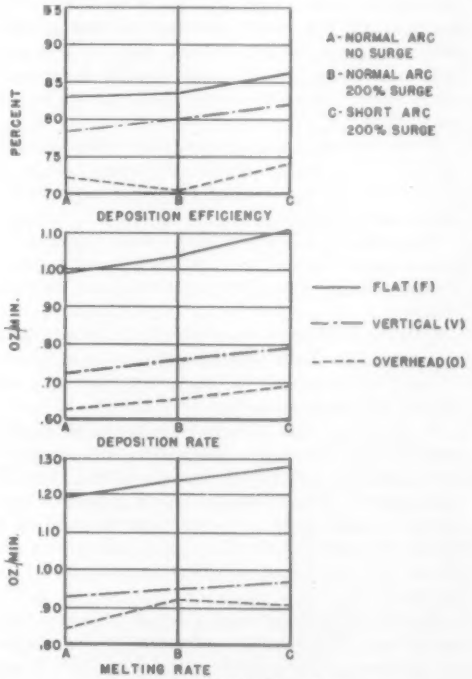


Fig. 16 E6010 electrodes

Table 7—Effect of Surge on Physical Properties

A...No surge; normal arc
B...200% surge; normal arc
C...200% surge; short arc

F...Downhand position—$^{3}/_{16}$-in. electrode
V...Vertical position—$^{5}/_{32}$-in. electrode
O...Overhead position—$^{5}/_{32}$-in. electrode

Plate No.	*Specification No.*	*Density*	*Yield point, lb./sq. in.*	*Tensile strength, lb./sq. in.*	*Elongation per cent in 2 in.*
			E4510 electrode		
1-A-F	1	7.731	49,875	67,375	12.0
1-B-F	2	7.735	53,250	69,000	9.3
1-C-F	3	7.646	56,000	65,875	5.3
1-A-V	4	7.561	40,875	55,675	8.0
1-B-V	5	7.584	42,250	57,000	6.5
1-C-V	6	7.601	46,125	53,625	5.5
1-A-O	7	7.653	45,375	54,000	7.3
1-B-O	8	7.611	47,000	56,750	6.0
1-C-O	9	7.667	47,375	57,125	5.3
			E6010 electrode		
2-A-F	10	7.803	58,800	70,750	23.5
2-B-F	11	7.800	58,000	68,375	22.0
2-C-F	12	7.795	59,000	70,375	21.5
2-A-V	13	7.823	50,625	64,500	28.8
2-B-V	14	7.817	51,250	66,000	25.3
2-C-V	15	7.822	50,875	63,125	27.5
2-A-O	16	7.810	52,375	64,375	25.5
2-B-O	17	7.809	55,750	70,000	24.5
2-C-O	18	7.809	56,250	69,750	20.0

The effect of surge on the melting rate and deposition rate and efficiency of E4510 electrodes varied with the position of welding.

With normal arc voltages the deposition efficiency of flat welding decreased slightly with the presence of surge. Reducing the arc length below the normal value when welding with high surge currents, tends to increase the deposition efficiency again. In the vertical and overhead positions, surge had little or no effect on the deposition efficiency when welding at normal arc voltages. Reducing the arc voltage, when welding with high surge current, tends to increase the deposit efficiency slightly above the values obtained with normal arc voltages.

The deposition and melting rates for vertical position welding decreased with the addition of surge. Reducing the arc voltage when welding with surge in the circuit had no apparent effect.

In overhead welding there was a slight tendency for surge to improve both the deposition and melting rates. This condition was true for both normal and short arc lengths.

With the E6010 electrodes there was a definite tendency for surge, both with a normal and a short arc, to improve the deposit efficiency, deposition rate and melting rate in all positions of welding.

To study the effect of surge currents on the physical properties of the welds, testpieces were machined from the plates made for the deposition tests. Two all-weld metal 0.505-in. diameter tensile specimens were taken from each plate. Average values of yield point, tensile strength and ductility, as obtained from duplicate specimens are listed in Table 7 and plotted in Figs. 17, 18 and 19.

A single density specimen was also machined from each plate. The densities, calculated by the weight-measurement method, are also listed in Table 7 and shown graphically in Fig. 19.

The results of these tests indicated that surge currents had a scattered effect on the physical properties. A tendency toward an increase in yield point was obtained by the addition of surge, particularly with the E4510 electrodes. (The maximum variation for any given electrode and position of welding was 6125 psi.)

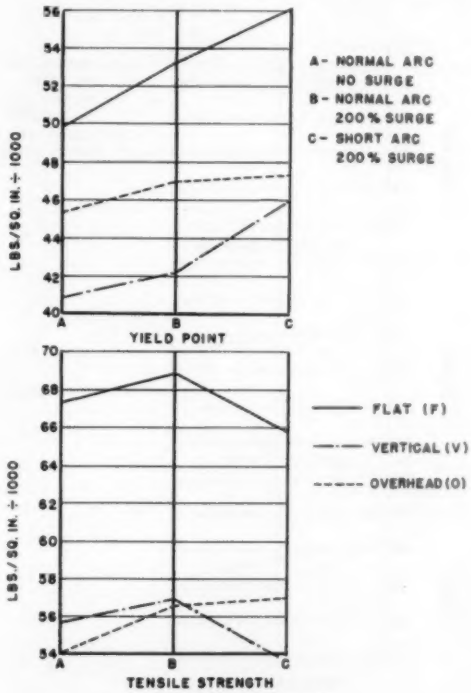


Fig. 17 E4510 electrodes

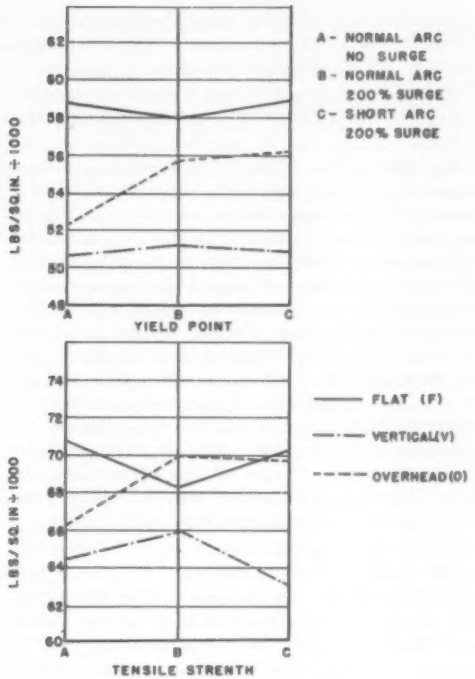


Fig. 18 E6010 electrodes

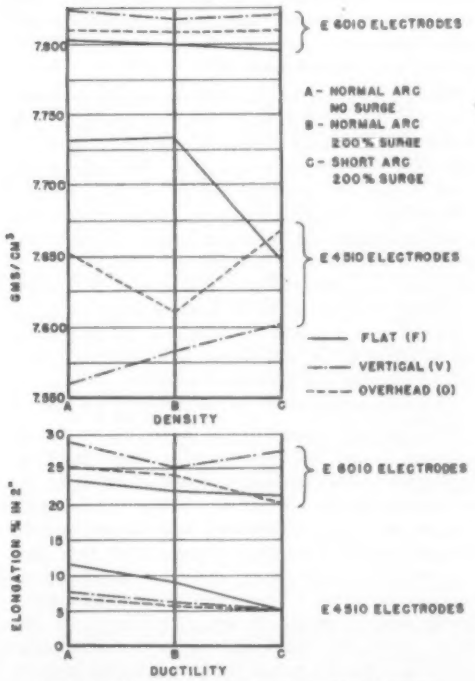


Fig. 19 Physical Properties as affected by surge

The tensile strengths in all but one case (flat position with E6010 electrodes) increased at normal arc voltages with the addition of surge. With shorter arc lengths, the tensile strengths remained about constant for overhead welding; decreased slightly for vertical welding; decreased for E4510 electrodes in the flat position and increased for the E6010 electrodes in the flat position.

Ductility values decreased in all cases with the presence of surge.

Relatively little effect was obtained on density with E6010 type electrodes although considerable variation was obtained with E4510 electrodes. For E4510 overhead welds, a marked decrease was obtained with surge at normal arc voltages, while an increase was obtained with surge at low arc voltages. On flat position, E4510 electrode welds, a marked decrease was obtained with surge at low arc voltages. Surge improved the density of vertical welds.

In all results of deposition and quality tests, with the exception of ductility, a consistent improvement was noted in the overhead position when the short arc and surge were present. Such improvement might be expected under difficult welding conditions because of the more easily controlled arc and better metal transfer made possible by the shorter arc.

ANALYSIS OF THE WELDING-MACHINE LOAD RELATIONS

The effect of surge currents, caused by the short circuits of the electrode occurring during the normal metal-arc welding process, have been shown to be beneficial under some conditions. The presence of surge currents, however, are generally accompanied by undershoots in current (Fig. 5) which occur as the short circuit is broken. Normally, the undershoot in current is increased in proportion to the surge current. It is also known that undershoot may effect the ability of a machine to maintain an arc at low-current densities, consequently, it is important to know more about it.

Before discussing undershoot it is important to consider the manner in which load current is determined by the welding generator, and to consider the variation in current with variations in load.

The static v.-amp. characteristic of a welding machine, as previously discussed, is shown as Curve 1 in Fig. 20. It defines the relation between the discrete values of terminal voltage of the welding machine and the current supplied by the machine, which relation must be satisfied for any given setting of the machine.

An arc load (or current-conducting path) connected across the welding-machine terminals also has a definite v.-amp. characteristic. The curve of the resistance load will be considered later.

The characteristic of the arc load depends on the linear length of the arc path. Curve *b*, Fig. 20, represents the characteristic of a welding arc of normal length. For the length represented, voltage and current

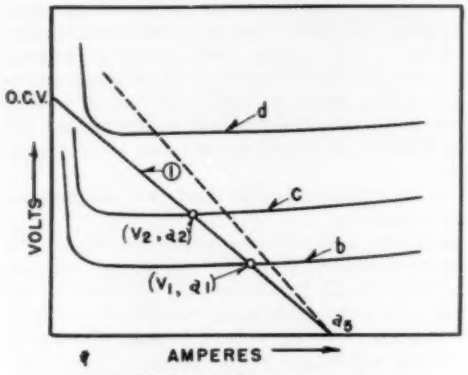


Fig. 20 Machine and arc v.-amp. curves

values for the arc must define a point on Curve *b*. The important feature of this characteristic is the substantially constant voltage over a wide range of currents.

In order that the welding machine may supply power to the arc some relation of current and voltage must be common to the characteristics of both the welding machine and the arc load; i.e., the characteristics must intersect at a point such as (v_1, a_1). The voltage v_1 is determined primarily by the length of the arc, and the current a_1 will be determined by the characteristic of the welding machine.

The longer the arc path, the higher is the voltage of the arc characteristic at any given current. Curves *c* and *d* of Fig. 20 represent arc characteristics for arc lengths of the order of three and ten times the arc length for Curve *b*. Since Curves 1 and *b*, and 1 and *c* have common points, the generator may supply power to the arc represented by *b* and *c*. The arc represented by *d*, however, cannot exist with the v.-amp. characteristic 1, since curves 1 and *d* do not intersect. Metal transfers from the electrode to the work, principally in the form of drops, which short circuit the welding machine and momentarily form a metallic path in place of the arc. Between these short-circuit periods, the arc normally is of such length that its characteristics approximate Curve *b* (Fig. 20). Frequently, however, even under ordinary welding conditions, the arc length may increase and the characteristic may rise to *c*. Less frequently and under adverse conditions, the characteristic may rise to a value such as *d*, the rise being due for example, to magnetic blow which may cause the arc to loop and increase the length of its path without substantially increasing the distance between the electrode and workpiece. The same condition might occur by the sudden transfer of an unusually large drop of weld metal from the electrode.

The importance of the slope of the v.-amp. characteristic of the generator at once becomes apparent, e.g., the steeper the slope of the characteristic for a given current setting, the longer the arc which may be maintained by the generator and therefore, the more satisfactory will the welding machine be under adverse conditions which may tend to lengthen the arc path. Curve 1 (Fig. 20) will intersect arc characteristics almost up to Curve *d*. If the generator characteristic was steeper, as represented by the dotted line, it would also intersect *d* and even higher curves, thereby allowing longer arcs to exist.

The slope of the v.-amp. characteristic may vary momentarily during, and for a short period, after a change in load voltage, such as occurs with a change in arc length. For this reason, v.-amp. curves are classified as static, which determine the v.-amp. relation after steady conditions are reached, and dynamic, which determines the relation between voltage and current while they are changing. The classification of dynamic characteristics may be further subdivided according to the property of the power source which principally affects them. As the term "dynamic characteristic" is ordinarily used, it applies to an effect peculiar to self-regulating generators alone, and that meaning will be used in this study. The second type will be referred to as an "inductive characteristic" since it is the result of the inductance possessed by all welding machines.

For the sake of explanation, let Curve 1, (Fig. 20) be the static v.-amp. characteristic of a constant-potential generator. As the name implies, the voltage of the generator proper is substantially constant over a wide range of currents. A resistance is connected in series with the generator, producing a counter voltage which is proportional to the current and which subtracts from the voltage of the generator proper. The net result is a terminal voltage which decreases as the current increases, producing the steep slope of the static v.-amp. characteristic.

The inductance of the generator (plus any inductance which may be added in series with the generator) will produce an additional voltage or counter voltage, but only while the current is changing. The voltage induced in inductive windings will add to the generator voltage with decreasing current and subtract from the generator voltage with increasing current. In each case, the effect is to produce voltages which tend to retard and which are proportional to the change in current. Conversely, the current in the welding circuit, which is composed of a generator, a resistance, an inductance and an arc in series, will change at a sufficiently high rate to provide any additional inductive voltage, or counter voltage required from the circuit by the load.

When the load voltage, across the constant-potential generator (equal to the terminal voltage) increases slowly, the current tends to decrease according to the static characteristic. When the increase is rapid, however, the inductive voltages add to the generator voltage and produce a terminal voltage higher than that required by the static characteristic for the instantaneous value of current. The v.-amp. relations during and shortly after a change do not, therefore, follow the static curve, but follow an inductive characteristic above it. The divergence of the two curves depends upon the amount of inductance in the circuit, the

current flowing and the rate of change of load voltage.

Consequently, an arc whose characteristic does not intersect the static curve of the constant potential generator may exist by virtue of the inductance of the generator or welding circuit. It may exist only momentarily, however, since the change in current which produces the inductive voltages is limited by the static curve of the constant potential generator.

Decreases in load voltage produce inductive characteristics below the static curve of the type previously considered under the analysis of surge or overshoot.

The basic difference in the constant-potential set and the self-regulating generator lies in the method of producing voltage regulation or decreasing terminal voltage with increasing currents. In the first case, it is accomplished by a resistance drop which subtracts from a constant-generated voltage. In the self-regulating generator, the generated voltage itself is internally caused to decrease with increasing current, producing a static v.-amp. curve which, in the operating range, is roughly similar to that of the constant-potential set.

The generated voltage of the self-regulating machine cannot faithfully follow rapid changes in load voltage such as occur in the welding process, but lags somewhat behind. It is this fact which is responsible for the dynamic v.-amp. characteristics which are to be considered.

Let the static v.-amp. curve again be Curve 1, (Fig. 20) and let the load be the arc whose characteristic is b. The generated voltage (neglecting resistance drop for the present) will be equal to v_1. Assuming a sudden change in arc length so that the characteristic rises to c, the load voltage must rise accordingly. However, since the generated voltage lags behind, it could not in itself supply current to the load at this higher voltage. As a result, the current tends to stop flowing. As the current decreases, however, sufficient inductive voltage is produced so that, added to the generated voltage, the sum is great enough to equal v_2 and to keep the current flowing, but at a decreasing rate. The generated voltage is rising in the meantime, but the current continues to decrease to supply the difference between the generated voltage and the arc characteristic c. When the generated voltage is great enough to supply current by itself against the load voltage determined by c, the current stops decreasing. Then it gradually builds up to the static value as the generated voltage rises slightly above the load voltage to compensate for the now reversed inductive voltage. The variation in current with time is similar to that shown in Fig. 21. (Also see Fig. 5.)

The transient decreases in current below the static value for the load voltage is known as undershoot and is the opposite of the surge and overshoot current, previously discussed.

The common method of measuring undershoot, as previously discussed, is to record the current variations by means of an oscillograph during and shortly after the load is changed from short circuit, or zero resistance,

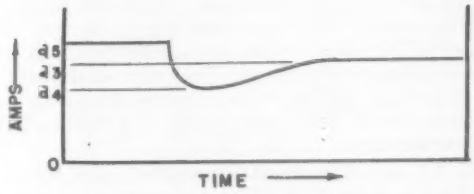


Fig. 21 Current variation from short circuit to welding conditions

to a resistance which provides a terminal voltage equal to the normal arc voltage (Fig. 5). The disadvantage of the resistance load lies in the fact that the load voltage changes in direct proportion to the current. As the current undershoots, the load voltage decreases in proportion, the difference between load voltage and generated voltage decreases much more rapidly than with an arc load. Consequently, the amount the current must decrease to supply inductive voltages for the difference is considerably less. However, it will be shown that measurements made with a resistance load may be used with a fair degree of accuracy for predicting the amount of undershoot which would appear with an arc load.

Let Curve 1 (Fig. 22) represent the static v.-amp. curve of the generator under test. The resistance load is adjusted to give the terminal voltage v_3 and the current will be a_3 under static conditions. A short-circuiting switch across the resistance is then closed ready for taking the measurements. The oscillograph is started, the switch is opened and the current variations are recorded.

A sketch of the current time oscillogram is shown in Fig. 21. As the switch is opened, the current drops from the steady short-circuit value a_5, undershoots to the minimum value a_4, then more gradually rises to the steady value a_3.

The v.-amp. relations during the change are shown in Fig. 22. Starting from short-circuit conditions

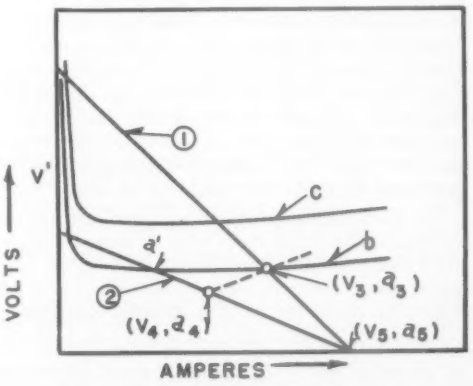


Fig. 22 Minimum dynamic v.-amp. curve

(v_5, a_5) the v.-amp. relation rises along a steep inductive characteristic (the slope depending on the rate the current is broken by the short-circuit switch) to the dotted line which represents the v.-amp. characteristic of the load resistance.* As the current drops to a_4, and then rises to a_3, the voltage drops in proportion to v_4 and rises to v_3.

For any other value of load resistance, there will be a load characteristic of different slope and corresponding changes in terminal voltage will occur. All changes and rates of change of current and voltages will be proportional to the change in terminal voltage and minimum values of current and voltage will define points which lie on a straight line through the point from which the change originated. Curve 2, Fig. 22 is the line which applies to the above example.

If the change has been from short circuit to an arc load with a v.-amp. characteristic such as *b*, the load voltage would have remained constant after the short circuit was broken and while the subsequent changes in generated voltage, inductive voltage and current were taking place. The v.-amp. relations in this case follow Curve *b* after the change from short circuit to the load characteristic is complete, and until static conditions are reached. Since the load voltage remains constant in this case, a proportionately greater decrease in current would be expected. The minimum current then would be very nearly given by the intersection of curves *b* and 2. Similarly, the minimum current is determined by Curve 2 for changes from short circuit to higher or lower arc characteristics.

The changes considered above were from short circuit to a higher load characteristic. Had the change been from a load characteristic above short circuit to a still higher curve, similar conditions would have existed but the line determining minimum values would have been above 2, through the point from which the change originated and parallel to 2.

If the change from short circuit to arc voltage occurs so soon after the short circuit was affected that the generated voltage has not decreased to the static short-circuit value, then the line determining minimum undershoot currents would not be identical with Curve 2. It would be parallel to 2, but would be higher and in between 2 and the static v.-amp. curve; its exact location depending on the amount the generated voltage had decreased when the short circuit was broken. Maximum undershoot in the welding process follows only those infrequent short circuits (due to drop transfer, etc.) which are of sufficient duration to allow the generated voltage to reach static conditions.

The minimum values defined by Curve 2 are the same as those which would occur with a constant-potential set with a static characteristic equal to Curve 2. This curve, then, is truly a v.-amp. characteristic of the self-regulating generator. It is a dynamic characteristic since it defines transient values. It defines minimum values of current and voltage since it passes through the static short-circuit point. It will be referred to as the minimum dynamic characteristic.

The rate of change in the load characteristic has so far been assumed to be greater than the maximum rate of change of generated voltage. Most changes in the arc are of that nature. However, for slower rates of change the generated voltage may follow the load voltage more closely and maximum changes in current are not required. The curve defining minimum values in these cases will be some place between curves 1 and 2, approaching 1 as the rate of change becomes very slow.

The above discussion suggests the method by which the minimum dynamic characteristic and the undershoot with an arc load may be obtained from the ordinary oscillogram taken with a resistance load. Let Fig. 21 represent such an oscillogram. Points on the static v.-amp. curve are given by a_5 and a_3, which may be read from meters but are preferably taken from the oscillogram. Voltage v_3 (see Fig. 22) is usually specified, or may be read from a meter, or the static characteristic for the particular generator setting if it is available. The load characteristic is determined by a line through (v_3, a_3) and the origin. The value a_4 may be measured from the oscillogram and plotted on the load-characteristic curve, giving the point (v_4, a_4) which, together with the short-circuit point determines Curve 2, the minimum dynamic curve. The arc-load characteristic is substantially a horizontal straight line in the welding range and may be represented as such. A horizontal line may be drawn in place of Curve *b* at any voltage (not necessarily v_3) for which the undershoot is to be measured. The intersection of this line with the minimum dynamic curve determines the minimum current, *a*, at the intersection of curves 2 and *b*.

It should be emphasized, however, that the minimum current is far less important than the slope of the minimum dynamic characteristic. As mentioned before, the arc length varies considerably, and the arc voltage varies accordingly. For instance, the conditions might be such that the arc characteristic varied in position between curves *b* and *c* (Fig. 22). If a change occurs from short circuit to *b*, the current may fall very low but the arc will not be extinguished. If, however, the change is to a longer arc represented by *c*, which does not intersect Curve 2, the current will drop to zero and the arc will die out.

If the maximum variation in arc characteristic (arc length or voltage) is known, Curve 2 for a given generator setting will predict whether or not the arc may be consistently maintained with that generator and setting.

It should be noted that references to arc voltage are references to instantaneous values unless otherwise noted. Arc voltages ordinarily given are average values read by a voltmeter. For the short circuit periods due to metal transfer, the instantaneous value of arc voltage is zero. Between these, the voltage is de-

* The resistance characteristic is a straight line through the origin, since the terminal voltage is proportional to the current, and through (v_3, a_3) a known point.

termined by the instantaneous arc length, which itself may vary considerably. The voltmeter averages the voltage over a relatively long time, and its reading is not suitable for use in transient measurements.

TESTS TO DETERMINE EFFECT OF CURRENT UNDERSHOOT

Three important effects might be expected from undershoot in a welding generator:

1. The arc may be extinguished. Interrupting the welding process is undesirable and may produce porosity and poor penetration at the points of starting and stopping.
2. The energy of the arc may be decreased so that there is not sufficient heat to produce sound deposits and complete penetration at all points.
3. Prolonged periods of undershoot may cool the electrodes and cause deionization and consequent change in the arc characteristic.

In addition to knowing if these possibilities exist under ordinary conditions it is desirable to know something of the arc characteristic on which there is little experimental data under dynamic conditions.

Test apparatus was connected as in Fig. 23 (*A*). The generator was the constant-potential generator No. 17 adjusted to 66 v. open circuit. R_1 was the usual ballast resistance. R_2 was additional resistance across which was connected a cycle drum. The cycle drum acted as a rotating switch, short circuiting R_2 during part of a revolution. During this time the arc current was determined solely by R_1. During the rest of the revolution, R_2 was not short circuited and the arc current was determined by $R_1 + R_2$. Figure 23 (*B*) shows the variation of current with time as the resistance was varied by the cycle drum during welding.

The cathode-ray oscilloscope shown in Fig. 23 (*A*) was used to plot the variations of arc current with arc voltage. Voltage leads from the arc were connected directly to the vertical plates of the oscilloscope tube. The horizontal plates were connected to the current shunt through a d.-c. vacuum tube amplifier whose response was such that, following an instantaneous change in voltage at the input, the output voltage reached the steady value in less than 0.001 sec. and with less than 3% overshoot.

With this arrangement, voltage was measured on a vertical axis and current was simultaneously measured on a horizontal axis by the displacement of the fluorescent spot from the axes of zero voltage and current on the oscilloscope screen. The spot then plotted the v.-amp. relations of the arc under varying conditions.

R_1 was adjusted (with R_2 shorted out) to give a short-circuit current of 134 amp. R_2 was then set to give, when not shorted, a short-circuit current of 16 amp. The cycle drum was set to short circuit R_2 two-thirds of each revolution, and was operated at a minimum speed of 60 rpm. This timing gave maximum periods of high-arc current of $^2/_3$ sec., followed by a period of low current of $^1/_3$ sec. The controls for the automatic welding head were set at 18 v., determining the average arc voltage. The instantaneous arc voltage between short circuits due to metal transfer was approximately 27 v. The high arc current between short circuits was about 80 amp., low arc current was as low as 10 amp. Under the extreme conditions the average welding current was approximately 70 amp., at a welding speed of 4 in. per minute on $^1/_4$-in. plate.

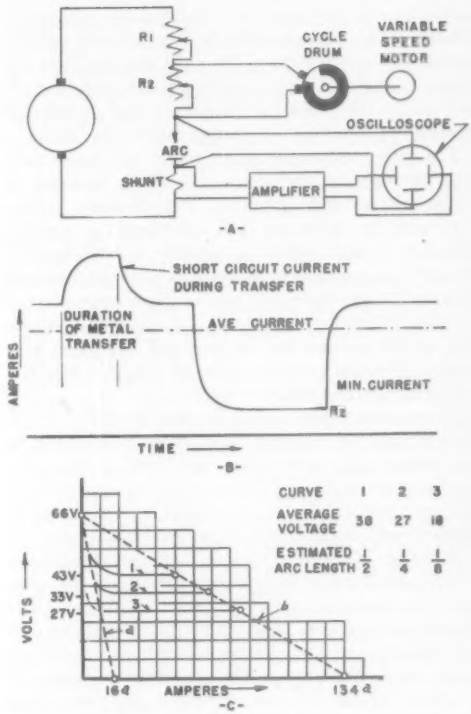


Fig. 23 Test circuit; voltage and current for maximum and minimum conditions

Stated briefly, the arc current undershot to 14% of the average welding current (a minimum of 10 amp.) for recurring periods lasting up to $^1/_3$ sec. The arc did not die out. Longer periods of undershoot were introduced by manually opening a switch (used in place of the cycle drum) across R_2. The arc did not die out for periods of two or three seconds, or the time required for the automatic head to drive the electrode into contact with the work, short circuiting the arc. This is material evidence that the arc is not extinguished or seriously deionized even by prolonged periods of very great undershoot.

With the cycle drum operating as before (minimum current of 10 amp. for $^1/_3$ sec.) the appearance of the arc and the weld was observed. The flickering and a regularly recurring hiss of the arc, due to the periods of undershoot, were quite noticeable. However, no change in crater size or in the molten metal was apparent. The

appearance of the crater and bead was not noticeably different from a bead run with the same average welding current but without droop. It is not expected that the quality of the weld would be affected, particularly when one recalls the whipping technique used in vertical welding to control the heat in the crater of the bead.

The entire experiment was repeated with higher currents between the periods of undershoot, resulting in higher average welding current, with the same results.

It must be concluded that undershoot in currents does not, in itself, affect arc stability or weld quality. It should be remembered that a series of closely grouped periods of undershoot will reduce the average current for the duration of the series. The occasional extinction of the arc encountered with self regulating generators, however, must be explained on the basis of the minimum dynamic characteristics.

It is apparent that, during the periods of low current, the arc path was not appreciably cooled or deionized by the surrounding atmosphere and masses of metal. Further confirmation of that fact was obtained on subsequent tests which were concerned with the v.-amp. characteristic of the arc under dynamic conditions.

Additional tests were made with R_1 and R_2 adjusted to give a maximum short-circuit current of 134 amp. and a minimum short-circuit current of 16 amp. The static v.-amp. curves for maximum and minimum current settings are shown as the dotted lines *a* and *b*, respectively, in Fig. 23 (*C*). The cycle drum was operated at about 120 rpm. and was adjusted to give two periods of minimum current each second and each lasting about $^1/_{12}$ sec.

As the current varied between the maximum and minimum values due to the switching action of the cycle drum, the oscilloscope traced the v.-amp. curve of the arc for the length maintained by the automatic head at the voltage for which the controls were set.

The position of the trace on the oscilloscope screen was observed with the aid of the auxiliary cross-section screen. While the path of the trace varied within wide limits as the arc length changed due to melting off of the electrode, the individual traces were well defined. The average position was noted and transferred to cross-section paper, after which the various points were rechecked by comparison with the oscilloscope trace.

The characteristic curves for the average arc voltages of 18 v., 27 v. and 38 v. are sketched in Fig. 23 (*C*). The linear values of arc length given at the side were estimated by comparison with steel pieces of known thickness held near the arc. The actual voltage for each characteristic was calculated by comparison with the open-circuit value and is given on the voltage scale to the left of the graph.

A photograph of the oscilloscope screen while the average arc voltage was 18 v. is reproduced in Fig. 24. The voltage and current axes have been supplied by retouching in this and in other photographs. In this case, several traces of v.-amp. curves are superimposed on the average path, each slightly different as the arc length varies as explained above. Short circuits due to

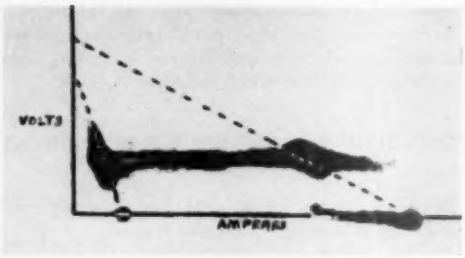


Fig. 24 View of Oscilloscope screen showing V-amp. characteristic of arc load between maximum and minimum current setting

metal transfer during the exposure are apparent as the trace registers zero voltage. It is these short-circuit periods which cause the average voltage to be less than the actual arc voltage. Points on the static slopes at maximum and minimum currents are also apparent from their intensity. They are, of course, the points at which the current and voltage settled and remained until the next change.

The v.-amp. Curve 3, Fig. 23 (*C*), taken at an average arc voltage of 18 v. is representative of the requirements of the E4510 electrode welding arc under normal conditions. The voltage does not vary proportionately with small changes in linear arc length, as a comparison with curves 1 and 2 will show. But the number and duration of short circuits in the arc due to drops of electrode metal increases rapidly with decreasing arc voltages.

Since those periods in which the arc is not short circuited are of principal concern in this test, it can be concluded that Curve 3 in Fig. 23 (*C*) represents normal requirements which must be satisfied by the v.-amp. characteristics of the generator.

Curves 1 and 2 are interesting since disturbances such as blow may temporarily increase the arc length to those values for which the characteristics are representative. The sharp upturn at low currents is apparent.

The time the minimum current existed was varied above and below the $^1/_{12}$ sec. specified for the experiment. Had deionization of the arc path due to undershoot occurred, three effects would have been apparent for each period of low currents:

1. The oscilloscope trace would have risen along Curve *a*, Fig. 23 (*C*) during the period indicating increased arc resistance.
2. The return path of the trace would have started at the higher point on Curve *a* when the current increased at the end of the period.
3. The amount of rise would have increased in each case as the periods were lengthened.

The movements of the oscilloscope trace could be readily followed in sequence. None of the regular variations listed above were observed, though periods of undershoot up to $^1/_3$ sec. were introduced. Random variations at infrequent intervals were observed and

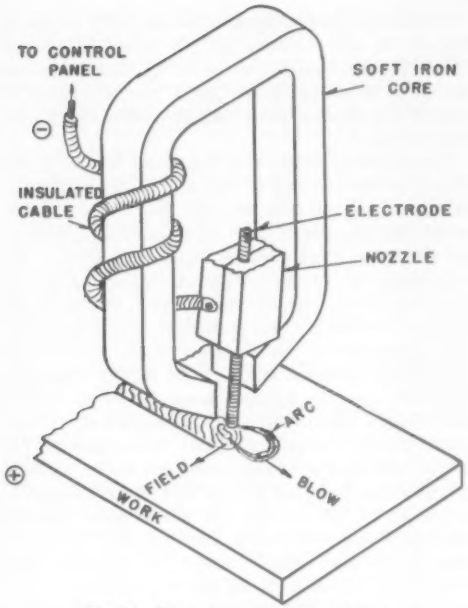


Fig. 25 Magnet to produce arc blow

are recorded in Fig. 24. But these were apparently due to changes in arc length from drop transfer.

IMPORTANCE OF INDUCTIVE AND DYNAMIC SLOPE CHARACTERISTICS

It is apparent that, under normal arc conditions, droop does not affect the arc with the possible exception of extreme values which may reduce the average energy. This fact is borne out in practice where the most difficulty in maintaining an otherwise satisfactory arc is encountered under conditions of blow. Consequently, the arc was studied under a transverse magnetic field, roughly proportional to arc current, to simulate the blowing effect encountered under actual conditions. Arc blow is the result of distortion of the magnetic field of the arc due to changes in cross section of the iron along the seam or to the direction of flow of current through the work. The unsymmetrical field has a motor effect on the arc, producing a resultant force on the flexible arc stream which stretches it into a loop. The longer arc path has a characteristic of higher average voltage which must intersect the minimum characteristic of the generator if an arc is to be consistently maintained.

The magnet was constructed and located with respect to the arc as shown in Fig. 25. The core was $^3/_{16}$ × 1-in. cold-rolled steel. The poles were spaced 1 in. apart and from $^1/_2$ to $^3/_4$ in. above the weld. A winding carrying arc current was used and three turns of cable produced the desired results. One side of the pole faces was located flush with the electrode and the series winding was connected to force the arc away from the magnet, thereby causing it to blow into a region of lower field strength.

The oscilloscope was connected as shown in Fig. 23 (*A*) to trace v.-amp. curves. In obtaining the curves shown in Fig. 23 (*C*) and in Fig. 24 the arc length was held substantially constant so that the curves are the characteristic curves of the arc at constant length. In this case the arc length is changing so that the oscilloscope traces the current response of the generator at varying voltages; i.e., the v.-amp. characteristic of the generator. The rate of change of voltage varies and different characteristics are accordingly obtained.

For volt time curves the voltage leads were connected to the vertical plates of the oscilloscope tube and the sweep circuit was used on the horizontal time axis. Figure 26 shows reproductions of photographs of v.-amp. and v.-time curves.

While a number of photographs were taken, the unpredictable nature of the arc changes makes a picture of any given type of change a matter of chance. Only one type of curve could be recorded at any instant. However, by comparing the three variables involved, the two types of curves taken under similar conditions could be chosen. Identical conditions are not necessary since limiting values are primarily desired.

The calibration of the oscilloscope at the time the photographs were taken was as follows:

Voltage	51	v. per inch
Current	60	amp. per inch
Time	0.016	sec. per inch

The auxiliary cross-sectional screen of the oscilloscope has 10 divisions per inch.

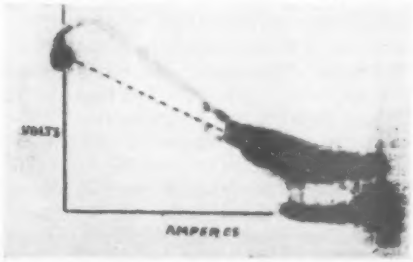


Fig. 26A Volt-Ampere curve obtained by Oscilloscope

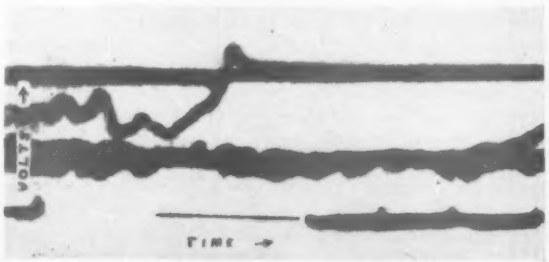


Fig. 26B Volt-time curves obtained by Oscilloscope

The constant-potential set was studied first to verify the theories set forth in the introduction on the relation between the v.-amp. characteristics of generator and arc and to be able to name the characteristic of the constant-potential set which determines the most adverse conditions under which an arc may be consistently maintained. These facts are necessary to postulate limiting values for the self-regulating generator.

In order to more readily interpret photographs such as those in Fig. 26, and to aid in making comments on them, a sequence of events is pictured in Fig. 27 in a series of graphs similar to the photographs. Figure 27 (*A*) shows the v.-amp. relations which occur during the changes and in which we are particularly interested. Figures 27 (*B*) and 27 (*C*) show corresponding voltage time and current time relations. The three graphs are located to place the voltage and current variables common to each pair of graphs on the same axis. Corresponding points are designated by the same letter in all three graphs.

A sequence of common changes occurring in welding under conditions producing arc blow are illustrated in the graphs as follows:

In the process of striking the arc the electrode is brought into contact with the work at *AB*, and withdrawn, establishing an arc at *CD*. A drop of molten metal from the end of the electrode bridges electrode and work at *EF*, short circuiting the arc. The short circuit is broken at *JK* and the arc re-established. Magnetic blow stretches the arc rather rapidly at *EG*, the arc path recovering to normal at *GH*. Blow again stretches the arc, but more slowly this time at *EL*. At *L* the arc becomes unstable under the effect of blow, stretches very rapidly to *I*, where it breaks.

The rate of change of arc voltage is evident in Fig. 27 (*B*) and the rate at which the current follows the changes is illustrated in 27 (*C*). Figure 27 (*A*) shows the actual v.-amp. relations during the changes.

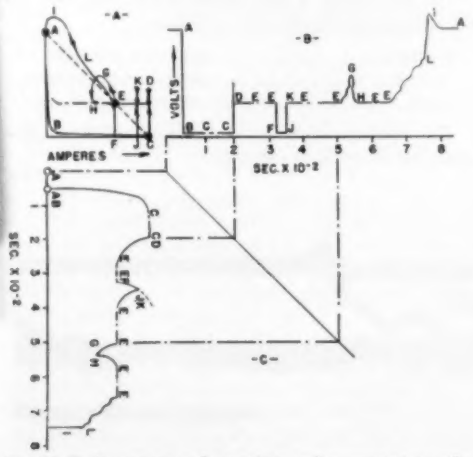


Fig. 27 Volt-ampere, voltage-time and current-time relationships

In studying the photographs similar to the graphs in Fig. 27 (*B*) and involving time, it should be remembered that the recurrent sweep of the oscilloscope has the effect of chopping the graph into a number of lengths and superimposing each successive length over the last one.

The incidents which were illustrated in Fig. 27 are typical of those whose voltage and current variations were observed and recorded, and illustrate the ability of the constant-potential set to respond to, as well as the means by which it meets, various conditions. Instantaneous changes in voltage are represented by vertical lines on the voltage-time curves as in Fig. 27 (*B*) and are followed by very high rates of change in current, indicated by the steepness of the current-time curve from the point at which the change originated. However, since the voltage changes before the current can change appreciably, the corresponding v.-amp. curve follows a vertical inductive characteristic. Examples are found at the beginning and end of the short circuit caused by metal transfer and during changes represented by *AB* and *GH*. It will be noted that the changes before and after a drop transfer, through at least part of their range, always follow a vertical characteristic; no points on the static characteristic exist between a minimum arc voltage only slightly below normal and short circuit.

At the instant the short circuit is broken the voltage instantly rises considerably above the normal arc characteristic, such as *HD*, to reignite the arc whose path was deionized during the short circuit, and immediately drops back to the normal curve. The whole action occurs so rapidly that the camera-oscilloscope combination could not record it clearly, but it could be observed with the eye and is present on voltage-time curves recorded on a mirror-oscillograph. The energy for such changes, as well as similar variations in arc characteristics due possibly to volatilization of inclusions in the metal of electrode or work, is supplied by the inductance. Even though voltages are encountered above the limit which the static curve could supply, the inductance of any normal generator is sufficient to supply the extra energy for the extremely short periods involved.

At measurable rates of change of voltage such as those indicated by *EG* and *EL* in Fig. 27 (*B*) the slope of the corresponding v.-amp. curve in 27 (*A*) approaches that of the static curve, the slower the change, the nearer inductive and static slopes become.

Increasing voltages always follow an inductive curve above the static characteristic; decreasing voltages follow a curve below. While the inductance of the generator may permit v.-amp. relations below and to the left of the static curve, comparable to following a flatter characteristic, the fact cannot in any way be considered a limitation of the generator, for such relations occur only during or following a decrease in voltage, a change to more stable conditions, except in the extreme case of short circuit treated in the discussion on surge currents. Therefore, only inductive characteristics

above the static curve indicating changes to less stable conditions will be considered.

Inductive characteristics, because of their steepness, may intersect higher arc characteristics than the static curve; i.e., they may make longer arcs possible, but only until the energy of the inductance is dissipated in the arc. If the rate of increase is sufficiently low or if the change maintained for a sufficiently long period, the inductive energy is used and only arcs whose characteristics intersect the static curve may be maintained. Under these conditions the static curve is the limiting factor in consistently maintaining the arc, and if the conditions do exist while welding, they will show as v.-amp. relations which closely follow the static curve or which return, either to the static curve or to open-circuit conditions as the arc is either maintained or broken.

The photographs in Fig. 26 (*A*) show variations which do follow the static characteristic indicated by the dotted line. An indication of the length of time the long arc path may exist, resulting in voltages considerably above normal, but still within limits which may be supplied by the generator operating on its static curve, may be found in the volt time curves in Fig. 26 (*B*), where the trace rises to near open-circuit voltages for periods up to 0.03 sec., a period which would require impracticable amounts of inductance to maintain greater voltages.

More rapid voltage changes from which the arc recovers are indicated by loops such as *EGH* in Fig. 27 (*A*), an example of which may be seen in Fig. 26 (*A*). Much larger loops have been observed occurring sometimes in rapid succession and with those parts of the loop indicated by *EG* and *HE* in 27 (*A*) deviating widely from the static curve. The extent to which the arc length may increase and still recover to normal voltages under the influence of inductive voltages is shown by the near vertical lines in the photograph in Fig. 28 taken while welding with the highly inductive No. 2 constant potential generator. The return of the voltage to normal values is too rapid to be recorded. A peak voltage was reached when the arc was broken, as shown by the voltage trace returning to the open-circuit value, but the maximum value depends on the rate of breaking.

Inductance, then, may be effective in decreasing the frequency of arc interruptions by supplying the energy for momentary increases in voltage, but cannot, within reasonable limits eliminate those due to more prolonged changes. And since both short and long periods of increased arc length occur under the same magnetic forces, the static characteristic alone is effective in determining the ability of the constant potential generator to consistently maintain the arc under given conditions. Furthermore, the inductance of the constant potential set and self-regulating generator are comparable and rates and duration of changes indicated in the photographs are great enough that the current, if supplied by the self-regulating generator, would undershoot to the dynamic characteristic of

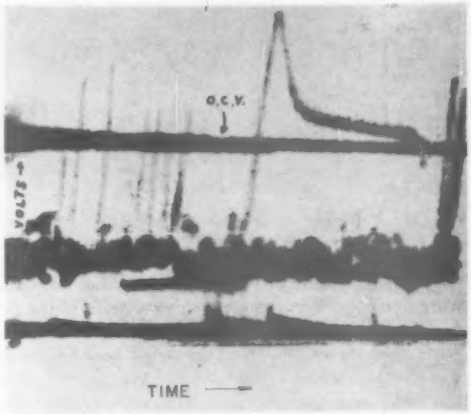


Fig. 28 Volt-time relationship on removing short circuit

minimum slope. There is sufficient evidence to postulate that in order that the self-regulating generator may be effective in maintaining the arc to the same extent as the constant potential set, the slope of the minimum dynamic characteristic must be equal to, or greater than the slope of the static characteristic of the constant-potential set at the same current.

DETERMINATION OF DYNAMIC CHARACTERISTICS

In Fig. 22 and the discussion which accompanies it, a method is suggested for determining the minimum dynamic slope of the self regulating generator from measurements obtained from a current time oscillogram. In the tests reported below, the actual v.-amp. curve is recorded for any specific change, giving a basic picture of the actual relations from which both methods are derived and at the same time, making simultaneous reading of voltage and current possible.

A typical 300-amp. self-regulating d.-c. welding generator was studied as an example of the type of machine under consideration. A cast-iron grid type of resistance was used as a load on the generator and was shunted by a heavy, manually-operated knife switch. The oscilloscope, connected as in Fig. 23 (*A*), traced the v.-amp. curve during and after a change, and the curve was photographically recorded on 35-mm. film, from the enlarged prints of which the measurements were taken.

The v.-amp. relations recorded during an exposure started at short circuit under static conditions; i.e., the switch shunted across the resistance load was closed. The switch was opened, allowing current to flow through the resistance for approximately $^1/_2$ sec., and closed again. A moment later the camera shutter was closed.

An example of the photographs obtained during the process is shown in Fig. 29. The current starts at the steady short-circuit value at 1. As the switch is opened

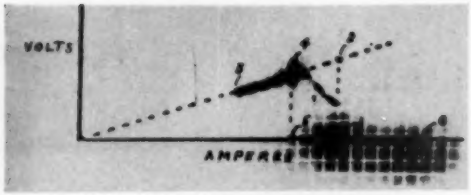


Fig. 29 Volt-ampere relationship directly after short circuit

the voltage instantly rises vertically a short distance with no change in current, probably a result of the formation of the cathode and anode spots of the arc at the contacts of the switch. Had the circuit been broken instantly, the voltage would have risen vertically to 2 on the resistance v.-amp. curve, but actually follows a slanting curve to the resistance characteristic just above 4 because the arc at the contacts decreases the rate of change. Following the resistance characteristic, the current decreases, with the voltage changing in proportion as required by the characteristic, to a minimum at 3 and subsequently recovers to 4, the steady values required by the intersection of the generator's static curve and load characteristic. As the switch is again closed, the reverse action occurs, with the v.-amp. relations dropping vertically to 5, overshooting to 6 and returning to steady values at 1.

Exposures were made at a number of different values of load resistance and measurements were taken from photographs and transferred to a common graph as in Fig. 30 (*A*). Points corresponding to 4 (Fig. 29) were connected to give a portion of the static characteristic (*a*). Points corresponding to 3 were connected to give the minimum dynamic characteristic (*b*) which was extrapolated to zero current.

By replacing the switch and resistance with the arc, the v.-amp. relations while welding with the same generator setting were photographed. The print is reproduced in Fig. 31 (*A*). The average path (i.e., the arc characteristic within the limits of current variation) is transferred to Fig. 30 (*A*) as the line *c*. Figure 31 (*A*) is a photograph which records the lowest value of current observed, either visually or photographically, at that voltage. The lowest value (1) of current and the corresponding voltage on *c*, therefore, define a point on the minimum dynamic characteristic and illustrate how closely the characteristics determined with an arc load and with the usual resistance load coincide.

The minimum currents measured by the different methods, however, would vary considerably. If the undershoot were measured by the usual method with the voltage of *c* as the final voltage across the resistance the current would undershoot to the value identified by the numeral 3 in Fig. 30 (*A*). This is the current at the intersection of *b* and the resistance characteristic Curve *d*, neither of which are apparent in the current-time oscillograms from which the current measurements are ordinarily taken. Point 2 is the minimum value of current as determined from the intersection of the minimum dynamic characteristic, found either as above or as in Fig. 22 and *c*, which in practice may be supplied as a horizontal line at the desired voltage. The voltage used should be the instantaneous, rather than the average arc voltage. The current, represented by point 2 is 9.5% greater than the true value of minimum current, or about equal to the error which could be expected from the measurements, while the minimum current 3, which would be obtained in the usual manner is about 27% greater. Still greater error could be expected if the undershoot were measured with a final

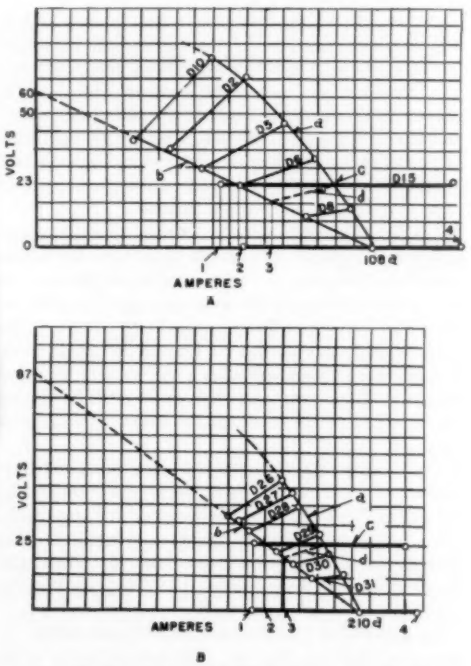


Fig. 30 Conditions for different resistance loads

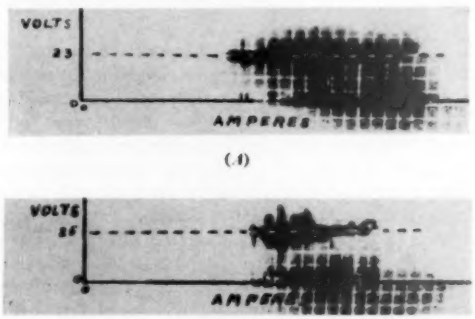


Fig. 31 Instantaneous changes between normal arc voltage and short circuit

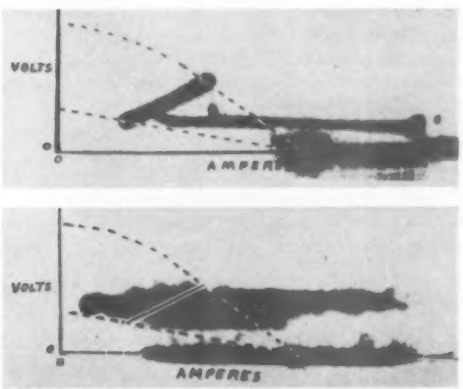
resistance-load voltage equal to the average arc voltage as is ordinarily done.

The graph in Fig. 30 (*B*) is similar in every respect to that in 31 (*A*) except that the generator setting was such that the short circuit current was 210 amp. The arc characteristic *c*, in this case, was obtained from Fig. 31 (*B*) similar to 31 (*A*) except for current setting.

It was previously stated that all characteristics of minimum slope would be parallel though they might be shifted vertically by an amount corresponding to the value of the induced armature voltage at the start of the change. Thus, in Figs. 30 (*A*) and 30 (*B*) a line through 4 (the maximum current after a short circuit from the arc characteristic *c*) and through the intersection of *a* and *c* (the point from which the change originated) is approximately parallel to *b*. Similar parallel characteristics exist for any instantaneous value of armature voltage possible. The rectangles, formed by the always instantaneous changes between normal arc voltage and short circuit, are apparent in Figs. 31 (*A*) and 31 (*B*), as they were in similar photographs made while welding with the constant potential set. However, their length is increased by the shifting of the characteristic of minimum slope.

Further confirmation of the importance of the minimum characteristic is found in Figs. 32 (*A*) and 32 (*B*), corresponding to Figs. 29 and 31 (*A*), respectively, but for a generator with considerable droop. From Fig. 32 (*A*), that part of the resistance characteristic followed by voltage and current after the switch was opened has been transferred to Fig. 32 (*B*) and the corresponding minimum characteristic drawn in. The current, as predicted, drops to the intersection of the minimum characteristic and the arc curve, but in spite of this extremely low value the arc was maintained for comparatively long periods of time and a satisfactory weld was produced during those periods. However, the slightest disturbance which caused lengthening of the arc path

(*A*)



(*B*)

Fig. 32 Oscillogram showing determination of minimum droop (A) for resistance load and (B) for arc load

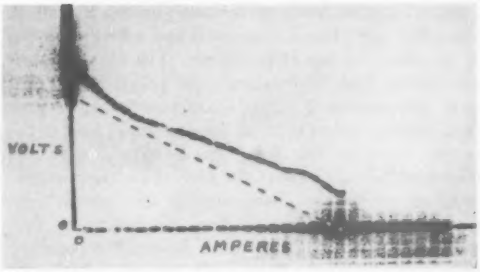


Fig. 33A Volt-ampere curve traced by the oscilloscope in changing from short circuit to open circuit conditions

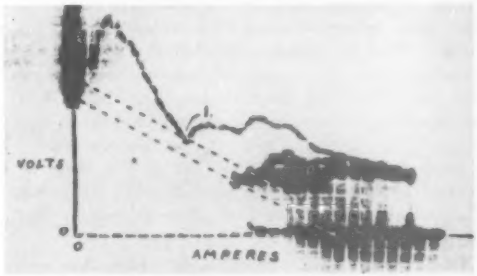


Fig. 33B Volt-ampere welding relationship with magnetic field present

sometimes caused the arc to die out, as would be expected from the low open-circuit voltage of the minimum curve. Again the primary reason for the limitation of undershoot, or rather the minimum characteristic which determines undershoot for any given set of conditions is the existence of factors which act to momentarily lengthen the arc path.

Figure 33 (*A*) is a v.-amp. curve traced by the oscilloscope as a change was made from short circuit to open-circuit conditions by manually opening a knife switch. The generator setting was the same as that for Fig. 30 (*A*), from which minimum Curve *b* is transferred to 33 (*A*). The rate of change of voltage as the arc was lengthened between the contacts was slow enough that the inductive effect had substantially died out, yet fast enough that the armature voltage could not approach the static curve. While the arc in this case is between copper contacts the picture shows the v.-amp. response of the generator under conditions which may exist in a welding arc. The closeness with which the minimum curve is followed indicates that it, rather than the static curve, is the limiting factor.

Figure 33 (*B*) shows the v.-amp. relations while welding with the same generator setting but with the magnetic field (Fig. 23) present. The change which is of particular interest is that in which the arc was stretched to the breaking point and the trace follows various directions at different rates of increase back to the open-circuit voltage. The minimum curve of the generator is drawn in. Unfortunately the change did not begin at static conditions. The parallel characteristic

of minimum slope, however, is drawn through the point from which the change originated and corresponds to the armature voltage at that time. The change must have started from short circuit, for points above the static, and possibly dynamic, characteristics on the arc curve are attained only on an increase rapid enough to provide inductive voltages. The voltage at first increased considerably above the normal arc curve, then remained constant or decreased as the current decreased and the v.-amp. relations came back to the dynamic curve of minimum slope through the point of origin of the change. Subsequent changes in voltage depended, of course, on change in arc length, which in this case again started to increase to the breaking point, and the voltage increased along an inductive curve to a peak value before dropping back to the open-circuit voltage. Had the change originated at a point on the minimum curve, the v.-amp. relations of the arc would have been limited by the minimum curve rather than the higher characteristic. The effect of inductance was, incidentally, more apparent on the v.-amp. curves of the self-regulating generator, but as with the constant-potential set, inductive voltages died out during changes of moderate duration, such as that in Fig. 33 (*B*).

The slope of the minimum characteristic does not change in proportion to welding current, but comparatively flatter slopes usually occur at generator settings for lower welding currents. Consequently, a generator which is satisfactory at higher current may not and usually will not compare with the constant-potential set in weldability at very low currents. Figure 30 (*A*) for instance, shows the characteristics of a 300-amp. self-excited generator set for about 100-amp. welding current. The open-circuit voltage of the minimum characteristic is nearly 60-v. At settings for lower currents, the o.c.v. would be less and the generator will not measure up to the standards of the constant-potential set, but this does not mean that the generator could not be used for welding with currents below 100 amp. The constant-potential set was operated at open-circuit voltages down to 40 v. before the steadiness of the arc began to decrease rapidly. Also the fact that the generator does not at all times operate on the minimum characteristic will decrease the frequency of, but not eliminate, arc interruptions.

As a matter of interest, it should be noted that for the same percentage of undershoot and with all other conditions equal, the slope of the minimum dynamic characteristic decreases as the slope of the static curve decreases.

CONCLUSIONS

There are many factors that combine to determine the welding characteristics of a d.-c. welding machine. The static v.-amp. characteristic is frequently used as a measure of a welding machine's performance, but it is not as important a factor as the dynamic characteristics. In this investigation, only two of the many possible dynamic characteristics have been considered.

Current overshoot or surge has been shown to have an important influence on the ability of a d.-c. welding machine to hold a short arc. This is important in vertical, overhead and deep groove welding.

The minimum dynamic characteristic, as determined by current droop has been shown to have an important influence on the ability of a d.-c. welding machine to maintain an arc at low electrode-current densities.

The establishment of exact values or limits of these important characteristics is impossible because experience indicates that there are many other factors that play an important part. For example, trouble from arc outage may be obtained for a given set of welding conditions with an E6010 electrode while no difficulty would be obtained with an E6012 or E6013 electrode. The improved stabilization of modern electrodes resulting from experience on a.-c. welding machines has partially neutralized the importance of the dynamic characteristics considered.

Excessive amount of current overshoot will increase arc noise and may increase spatter loss. Also as the overshoot of a self-regulating generator increases, the current droop also increases which results in lower minimum dynamic characteristics and poorer welding characteristics. The proper dynamic characteristic is a balance between the two.

Rapid recovery or response of the welding current to varying load conditions is of primary importance. It makes possible excellent welding characteristics with a minimum of both current overshoot and droop.

As more experience and knowledge is obtained, the characteristics of welding machines will be improved. With the present state of the art, however, it is still impossible to evaluate the welding characteristics of a welding machine entirely from electrical test. The welding operator, although not entirely infallible, is still the final test for welding characteristics.

ACKNOWLEDGMENTS

The writer wishes to acknowledge the assistance of A. B. White in conducting this investigation, and to express his appreciation to the Westinghouse Research Labs where this work was conducted.

Tubular Sections in Frame Design of Hydraulic Benders

Combined use of structural beams and pipes give the needed rigidity, radiating areas, weight and cost reduction and neat appearance in the construction of hydraulic benders

by *E. J. DeWitt, F. J. Lammers and F. D. Alexander*

WE HAVE been asked many times by people who have seen our modern machines with their clean lines engendered by the use of tubular sections for both stressed members and oil storage, why it took so long to evolve this particular style. Frankly, our organization is made up of men and, as men, we go through a period of evolution much the same way as our ancestors did.

For many years, we manufactured machines of all kinds and types that had to do with the forming of metals. We, at one time, made multiple slide machines (Fig. 1), special forming machines (Fig. 2), reinforcement bar bending machines, tubular bending machines and even, when the going was tough, did some custom machine work.

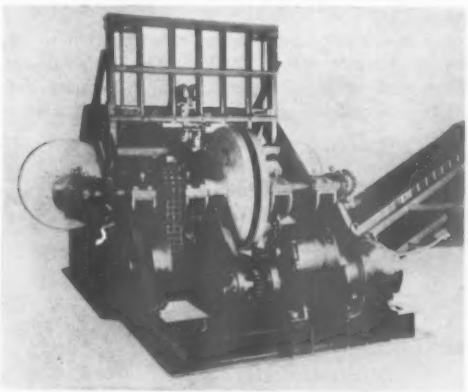


Fig. 1 Motor driven 5-slide machine with conveyor; capacity two bends $^{1}/_{16}$- x $^{5}/_{8}$-in. flat wire, 56,000 operations per hour

E. J. DeWitt, F. J. Lammers and F. D. Alexander are connected with the Wallace Supplies Mfg. Co., Chicago, Ill.

Presented at the Thirty-first Annual Meeting, A.W.S., Chicago, Ill., week of Oct. 22, 1950.

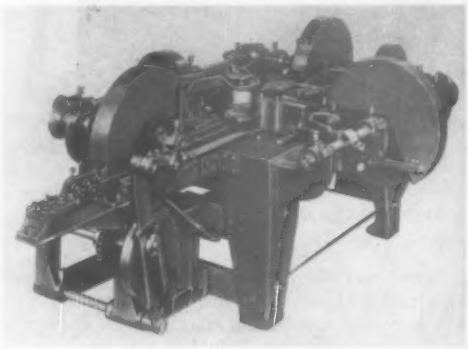


Fig. 2 Motor driven 4-slide wire forming machine

Jumping from one problem to another as we did, using large gear reduction drives to apply power (Figs. 3 and 5 are typical), it was inevitable that we tried various basic attacks in frame design. We learned something of advantage on each job; that the best frame or body unit for the machine would reflect and be functionally restricted by the end use of the equipment. A machine that needed a surface arrangement such as is shown in Figs. 1 and 3 could only evolve around a frame welded as shown in Fig. 4.

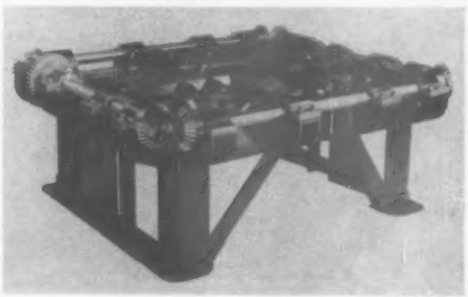


Fig. 3 Gear train assembled in frame for multiple 5-slide machine

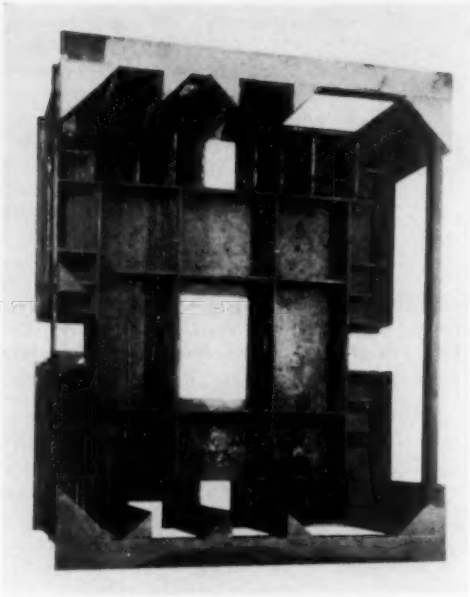


Fig. 4 Welded frame for multiple 5-slide machine

As we began to develop specialties, taking a page perhaps from the books of the medical profession and the law, more and more of our work fell into the line of bending tubing and pipe. In the early days, all our machines were built of castings, carried elaborate gear trains and even the simplest machine elements were cast. Now we really mean "simple," like clevises, locking blocks, etc. When a move was made to take us away from castings to the use of steel, it was done on the job, resulting in a first redesign such as Fig. 6. The modern counterpart of that machine is our new tubular 500-Y bender. In contrast, we show it in Fig. 7—sleek, slim, taking very little floor space and producing many times more units per hour of operation.

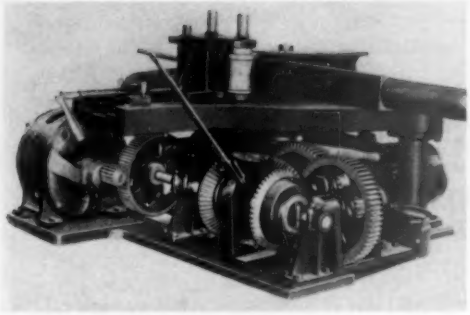


Fig. 5 Motor driven 4-in. capacity pipe bender

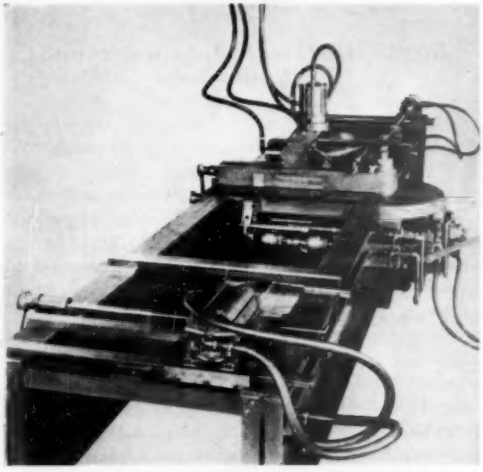


Fig. 6 Motor driven right- and left-hand tube bender with air-operated mandrel extractor unit

We would like to emphasize, however, that we did not just jump from the 1932 design to the 1950 design. We moved—cautiously, perhaps, but nonetheless definitely—away from gears, clutches, reversing motors,

Fig. 7 Hydraulic tube bender with electric controls (Wallace 500-Y furniture and auto seat frame bender)

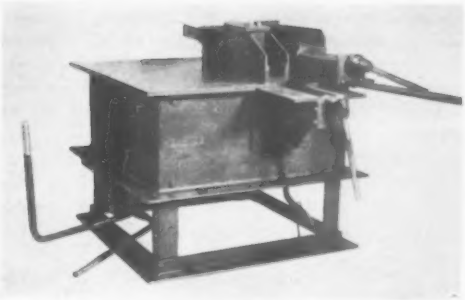


Fig. 8 Original hydraulic bender with frame both bolted and gas welded

brakes, to hydraulic power. The very first unit built to use hydraulic power was contained within a massive frame of 18-in. ship channels. This unit was first bolted together and then welded to insure rigidity. Since we could not then afford an arc welder, this massive frame was gas welded with the biggest tip we could find. A photograph of that machine is shown in Fig. 8. It was so arranged that for small work the direct lever could be pulled; for heavy work, the hydraulic hand-operated booster cylinder could be used.

By way of contrast and an initial stage in redevelopment, note the change from the geared unit shown in the 1932 design, Fig. 6, over to the basically new design concept of Fig. 9. To refresh your memories, we compare the 1932 design of Fig. 6 with the 1938 design of Fig. 9, so that the floor space savings can readily be seen.

After considerable practice, we found that it was not necessary, except in rare cases, to have right- and left-hand benders if clearance was provided to permit the pipe or tube to be turned either up or down. Consequently, we moved to a development of a hydraulic bender considerably more compact, more flexible, more

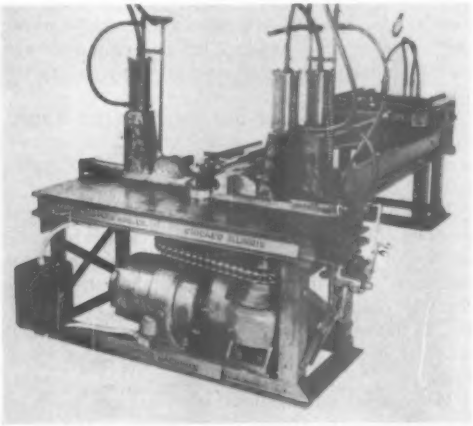


Fig. 9 Motor driven 3-in. capacity bender showing use of speed reducer drive replacing gear trains

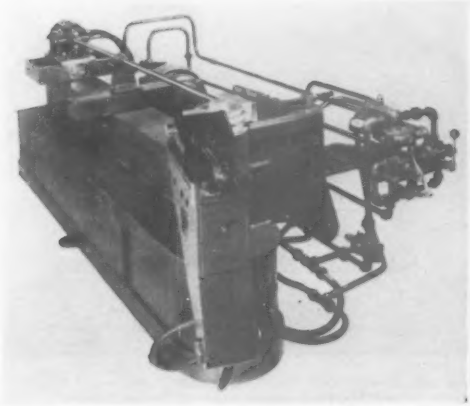


Fig. 10 All-welded hydraulic bender showing separate main frame and power unit

accurate and infinitely more durable. Such a machine is shown in Fig. 10.

Now we come to the complications caused by welding. These first welded hydraulic benders had a main frame unit and a subsidiary unit or power pack. These two components shown in the photograph had approximately 320 ft. of welding. Hence, all this welding brought together new problems of technique, of warpage and of clean appearance.

Because ours was a complex line and furthermore since we had literally destroyed all the patterns for castings and could not go backward, it took us an unusually long period (five years in fact) before we could get back to some of our earlier efforts and profit by the mistakes we made. Our secondary designs removed the separate power unit and brought a reduction in the number of feet of weld, but because of the compacting of the unit, other problems were introduced.

While the machine shown in Fig. 11 has greater capacity, its fundamental operating principle was not seri-

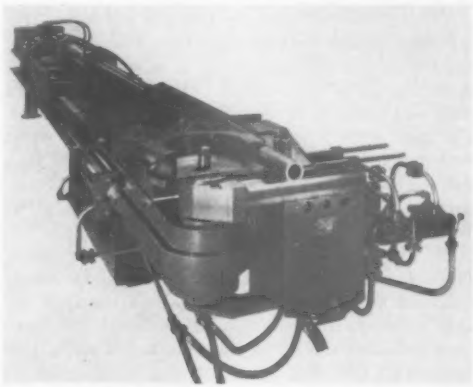


Fig. 11 All welded 4-in. XH I.P.S. S.A.E. 4140X capacity hydraulic bender with power unit combined into main frame and pusher unit

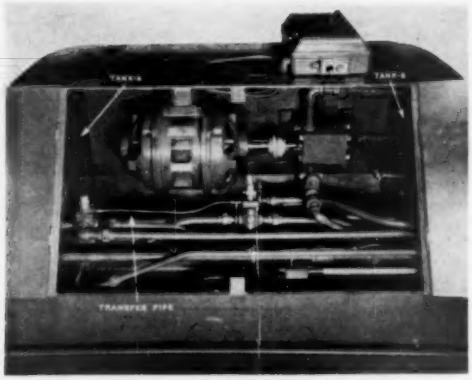


Fig. 12 Power unit showing separate oil tanks to overcome the problem of high oil temperatures

ously changed. This machine was built in 1940, carried a cylinder for forcing tube along with the pressure die to relieve tension on the clamp end and was developed for one of the eastern Navy yards. Its massiveness is apparent, yet it was compact enough to meet a floor space requirement.

Now, some of these first redesigned units ran somewhat higher oil temperatures than we felt desirable. This necessitated refinements in the oil storage area or tank. Oil reservoirs, constructed with baffles and opposed inlets and outlets, tend to make the oil flow more irregularly than it would in a straight tank. But, in spite of this—i.e., the baffles—the oil flow still continues to function very much as though it were going through a small, irregularly shaped pipe. This short circuit resulted in the same sections of oil being used over and over again. That undesirable feature we overcame by a further refinement which had two separate tanks connected (Fig. 12). This design made it necessary for one tank to empty at a given frequency into the other so that the operation could continue. The larger volume of the tank resulted in a larger radiating area, which resulted in more successful heat dissipation.

Although the heat transfer problem was solved, the net effect of this work brought back more welds and more fit-up and assembly problems, as well as difficulty in getting the oil tanks clean. Our rapidly increasing labor cost forced us to examine each detail in our units. The most prominent of these costs were those which were brought about by the excessive welds in the tank and the alignment problems which they caused in assembly. We carefully studied these problems, as we did not want to sacrifice any desirable hydraulic features. Many alternates became readily apparent. These were set up and checked for welding and assembly cost, with the end in view of our getting as nearly as possible an irreducible minimum of weld with the maximum radiating surface in the tank and a major flow distance for minimum short-circuiting in our hydraulic storage system.

The main member or backbone of our benders had been a wide flange beam (selected initially to save welding and assembly labor), stiff enough to be practically unyielding under the maximum stress occasioned by over-capacity use. Our tube benders have a rear mounted mandrel extractor unit and, as a consequence, we must have more machine length than there is in the tube to be bent. It became axiomatic with us to use standard steel sections as rolled rather than to make up and weld sections of special design. We have long since found that we cannot hope to compete with modern, continuous steel mill techniques in our own little shop.

Among the sections considered was large outside diameter pipe since, on a weight basis, it is the strongest section for our particular requirement. This answered part of our problem; it left the mounting of the drive unit and other parts more of a problem than ever.

The final answer to this was found by using the wide flange beam in the head portion of the unit and a pair of pipes for the remainder (Fig. 13). This gave ample rigidity for excellence of operation; the oil reservoir and storage problem was solved; the large radiating areas, rather than being sacrificed, were increased; the overall weight was reduced; assembly of the frame and base materially simplified; total footage of weld greatly reduced; appearance improved; more working area provided for the piping, valves, pump and motor; working height was lowered; and last, but not least, price was lowered during a time when most prices were going the other way.

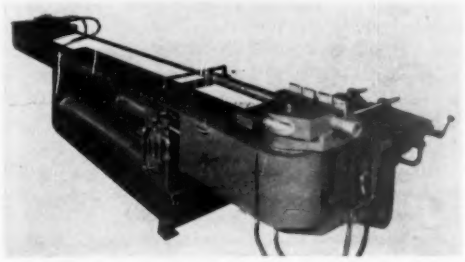


Fig. 13 Modern hydraulic bender with an all-welded frame made of large O.D. pipe and a wide flange beam

It can therefore be said that the adaptation of these changes solved many vexing problems within our own organization; and outside our organization it strengthened our competitive position.

These practices of using pipe or tubing for structural members of our machines has resulted in some fantastic savings of weight—particularly in the larger of our machines. Going back to Fig. 11 will show the massive character of these earlier heavy machines. In contrast, Fig. 14 illustrates a machine identical in capacity but infinitely cleaner of line and substantially lighter in weight.

The general specification for both these machines lists capacity as being able to bend $4^1/_2$-in. O.D. x $^5/_8$-in. wall S.A.E. 4145 steel tubing to a 9 in. center line ra-

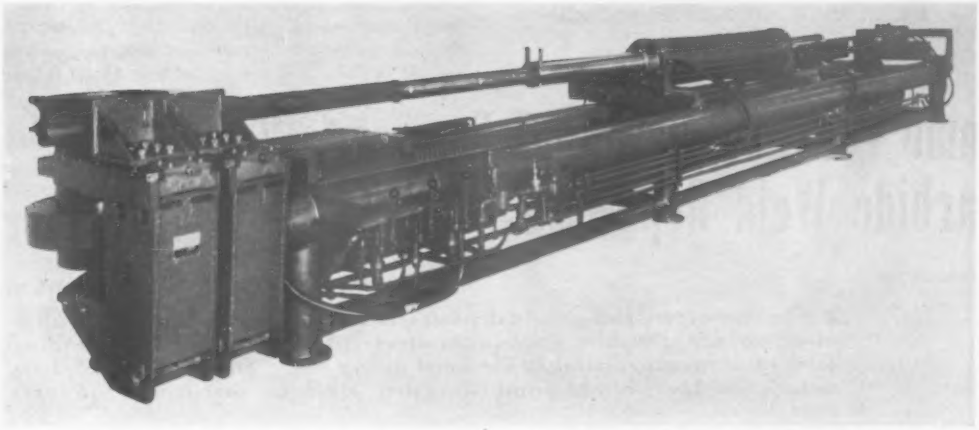


Fig. 14 Modern 4-in. XH I.P.S. S.A.E. 4140X capacity bender showing tubular frame serving as the oil reservoirs

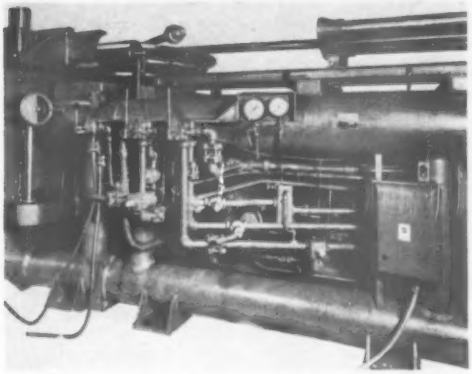


Fig. 15 Welding details of tubular frame for 8-in. XH I.P.S. capacity pipe bender

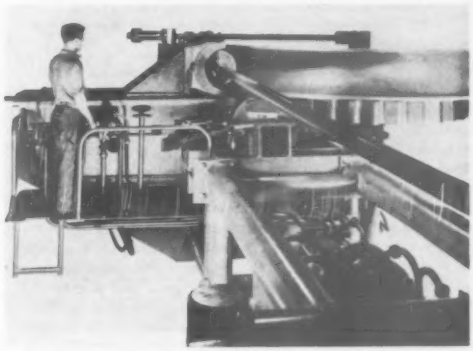


Fig. 16 Portion of tubular frame of a 17-in. capacity pipe bender (develops 20,000,000 I.P.T.)

dius. This same machine, used with special tooling, also bends 6-in. O.D. copper to $7^1/_2$ in. center line radius, with a wall as light as 0.062 in., having a tangent (straight section, that is, ahead of the bend) only 6 in. long.

Figure 15 shows details of welding on a tubular frame for a bender capable of bending 8-in. pipe ($8^5/_8$ in. O.D.) with a wall thickness of $^3/_4$ in. to a center line radius of 16 in. This bending is done cold with a continuous and steady flow of hydraulic power.

Simplicity of design is clearly evident in this closeup. The pipe members serve as oil reservoirs and the long tubular surface of these pipes provide maximum radiating surface for the ready cooling of the oil. The flexibility of this construction is evidenced in Fig. 16, which shows a portion of a larger bender for 17-in. pipe, $17^5/_8$ in. O.D. Here the highly stressed members are simple and easily connected.

This machine develops a torque of 20,000,000 in.-lb.

In summary, it is suggested that designers of machines, particularly hydraulic machines, investigate the flexibility of tubular sections from a design standpoint and, more especially, bear in mind that they may have a duplex function when used with hydraulic machines. We should like to close this paper with the same admonition given in a previous paper at Atlantic City some years ago. The way to reduce the problems generated by welding is simple; reduce the amount of welding. We are convinced that many of the complications that exist or tend to exist in welded machinery construction can be eliminated by the designer who makes a concerted effort to reduce welding *per se* to the minimum. Some ingenuity is required, but generally speaking, it is rewarding—rewarding because it reduces shop hours which in turn tend to reduce cost, which in turn tend to reduce selling price, which in turn tend to broaden markets. It has long been our contention that larger markets can be reached with the broadening effects of arc welding judiciously used.

Some Characteristics of Composite Tungsten Carbide Weld Deposits

The characteristics of weld deposits from rods made by enclosing tungsten carbide granules in a mild-steel tube provide an outstanding level of abrasion resistance for hard facing use. The matrix has characteristics that range from tungsten steel to cast-iron structures

by Howard S. Avery

Abstract

Weld deposits from composite rods made by enclosing tungsten carbide granules in a mild-steel tube provide an outstanding level of abrasion resistance for hard facing use. The sheath tubing melts during welding and dissolves both tungsten and carbon to form a hard matrix that anchors the very hard granules in place. Instead of being a mild steel binder for the tungsten carbide as is sometimes believed, this matrix has characteristics that range from those of tungsten steel to cast-iron structures containing considerable secondary tungsten-iron carbides.

Surface roughness of abraded deposits depends on initial granule size and welding procedures. Abrasion resistance depends largely on the volume of undissolved carbides, and is generally better for gas welds. However, the hot hardness of arc welds is higher at 1200° F. The Vickers micro hardness of the primary tungsten carbide is about 2400 Vpm. (25-gm. load), while the secondary carbide, $(Fe, W)_6C$, is near 1750 and the other matrix constituents are softer. Arc welding may largely dissolve the primary carbides, especially if they are very fine, and result in loss of their distinctive abrasion resistance. These relations are illustrated graphically and correlated with micro structures to aid the user of hard facings in evaluating field performance.

THIS paper is a description of certain metallurgical features of hard-facing deposits that are not well known and that may affect the performance of hard overlays made from composite tube rods containing tungsten carbide granules. Some features stem from manufacturing and specification factors while others are variables whose control is in the hands of the welder. Appreciation of these metallurgical details should make more effective the use of tungsten carbide hard facing.

Hard facing has two important advantages: (1) applicability at the spot where wear resistance is most needed and (2) easy use of very hard and abrasion resistant materials, which is especially advantageous when a tough shock absorbent base metal is required. There are many alloys to select from and there are many factors that may complicate the selection problem. However, if the properties of extreme toughness (such as are provided by low carbon-alloy steels or austenitic manganese steel), corrosion resistance (which is usually supplied by nickel base, cobalt base, or stainless steel alloys), heat resistance (which is generally insured by a minimum of 25% chromium) and hot hardness above 1100° F. (provided best by cobalt base alloys) are not considered essential, composite tungsten carbide deposits become a logical selection since from them is expected maximum abrasion resistance. There are few materials* that are harder and perhaps none currently available that combine such hardness with equivalent toughness.

Tungsten carbide is commercially available as sintered compacts produced by powder metallurgy (tool tips are a familiar example), as cast slugs or inserts, as granules produced by crushing cast materials, and as a component of composite welding rods. The last product is the subject of this discussion.

The commonest composite rod is a tube of mild steel filled with granulated carbide. Small proportions of other materials, such as arc stabilizers, binders and the like may be included. Different percentages of steel and filler are employed for some applications. These may be obtained by variations of tube thickness or by inclusion of iron powder in the filler. The latter is especially suitable if a low proportion of carbide granules is wanted.

The ratio of carbide to steel is a rather arbitrary selection. The A.W.S. 1950 *Welding Handbook* chapter on Filler Metal lists two types as follow:

Table 1—Deposited Metal from Surfacing Filler Metal

	C %, min.	Total other elements, %, minimum
Homogeneous deposit	1.8	45.0 incl. 5.0 W
Heterogeneous deposit	1.8	50.0 incl. 45.0 W

Howard S. Avery is Research Metallurgist, American Brake Shoe Co., Mahwah, N. J.

Presented at Thirty-first Annual Meeting, A.W.S., Chicago, Ill., week of Oct. 22, 1950.

* Diamond, boron carbide, silicon carbide and perhaps chromium boride and related compounds are harder.

The description above attempts to define end products that are considerably affected by welding technique and are expensive to check. Consequently it is more helpful to the user to describe the rods and the performance of resulting welds.

Current commercial practice tends to standardize on a 60% carbide: 40% tube ratio. A 37% carbide: 63% steel combination is also popular for some applications. Perhaps careful field testing could define various optimum ratios for different applications, but the uncontrolled variables in welding and service testing are so large that valid discrimination between ratios that differ slightly is usually impossible. For the controlled laboratory studies described later the 60:40 ratio has generally been employed.

Granule size of the filler is also subject to specification and designation. This factor controls roughness of the worn surface that develops in service and may also control matrix character, as will be shown later. The rough cast tungsten carbide is reduced to required sizes by conventional crushing equipment (causing considerable wear in the process) and then classified by screening. At this stage wear of the equipment may introduce minor amounts of iron with the granules. This iron could be removed magnetically if desired, but there is no technical justification for so doing, in spite of some claims that the absence of magnetic filler material has special virtues.

Iron oxides or other magnetic ingredients are sometimes intentionally incorporated in the filler to influence weldability. Occasional attempts to exploit the presence of these or magnetic particles from crusher wear are made with the purpose of arguing the superior wear resistance of one rod over another. There is no valid evidence either way on this matter. It should suffice to point out that the normal manufacturing variation of a per cent or so in the filler:tube ratio or the much larger deposit composition changes that occur during welding would overshadow and prevent the detection of such an effect if it existed.

The uninitiated may think of deposits from tungsten carbide tube rods as consisting of the hard granules in a tough, mild steel matrix. This differs from the true status, since the welding operation causes changes that are indicated by Fig. 1.

Molten iron can dissolve tungsten carbide as well as elemental carbon and tungsten metal. The mild steel tube may originally contain only 0.10–0.20% carbon, but when molten can pick up perhaps 1.5–2.0% carbon and much larger amounts of tungsten. Upon solidification carbon may be rejected from solution as cementite (iron carbide), or it may remain in solid solution.

In the absence of carbon, tungsten-iron compounds (tungstides) may separate from the solidifying melt containing these elements. However, in the presence of carbon the compounds that separate are usually complex carbides containing both iron and tungsten. These are very hard and relatively tough in comparison with iron carbide. The percentage of available carbon determines the amount of these carbides that will precipitate from W-Fe-C alloys on cooling; the percentage of tungsten controls their composition. The alloys may be typed as tungsten irons if the carbides appear freely and as tungsten steels if carbon is too low for their appearance.

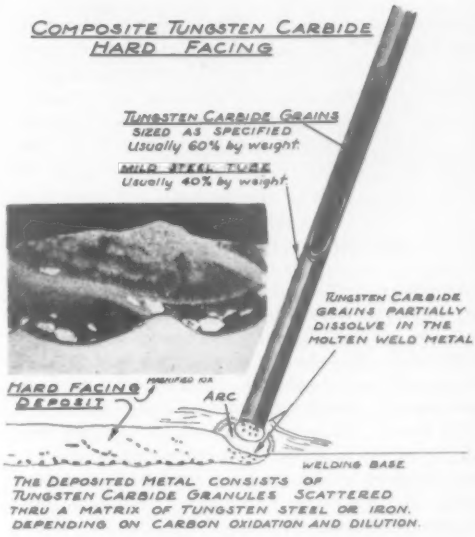

COMPOSITE TUNGSTEN CARBIDE HARD FACING
TUNGSTEN CARBIDE GRAINS
SIZED AS SPECIFIED
Usually 60% by weight
MILD STEEL TUBE
Usually 40% by weight
TUNGSTEN CARBIDE GRAINS PARTIALLY DISSOLVE IN THE MOLTEN WELD METAL
HARD FACING DEPOSIT
MAGNIFIED 10X
ARC
WELDING BASE
THE DEPOSITED METAL CONSISTS OF TUNGSTEN CARBIDE GRANULES SCATTERED THRU A MATRIX OF TUNGSTEN STEEL OR IRON, DEPENDING ON CARBON OXIDATION AND DILUTION.

Fig. 1 **(Reduced by 1/3 in reproduction)**

The molten weld metal from a steel tube containing tungsten carbide granules provides a setting for operation of the tendencies described above. The amount of tungsten carbide that dissolves depends on temperature, length of time in contact and the area of contact (which increases as the carbide particles become finer).

One limiting condition is approximately represented by brief low-temperature fusion in the oxyacetylene flame of tube rods containing very coarse tungsten carbide granules. (This procedure is used to prepare nonskid, wear-resistant horseshoes.) The other extreme is obtained when a tube rod filled with fine tungsten carbide powder is melted in a high amperage electric arc with no protection from air oxidation, considerable fusion and mixing in of the base metal, and comparatively long contact of granules and molten metal. Under these conditions all of the tungsten carbide can dissolve while at the same time considerable carbon is lost by oxidation, and the percentage further reduced through dilution by the lower carbon base metal. The result can be a tungsten steel containing few if any free carbides.

Between these extremes are a series of structures that provide a wide range of properties and performance. These are of considerable practical interest to those who select tungsten carbide hard facing for its wear resistance.

To report quantitatively the behavior across the range described above the two important properties of hardness and abrasion resistance have been selected for detailed study. Hardness is the usual first recourse in

estimating wear resistance. The implied correlation is frequently valid, but enough exceptions have been found through laboratory and field experience to indicate that hardness alone must be used with caution. Toughness and perhaps other less obvious properties are involved. Consequently, more direct wear tests are advisable if they are available and have proved reliable.

EXPERIMENTAL PROCEDURE

Hardness measurement is a simple matter for ordinary irons and steels. Rockwell and Brinell tests are rapid and generally familiar, and require little experimental skill. However, for substances as hard as tungsten carbide and the mineral abrasives with which it must contend, these tests have sharp limitations.

The Brinell ball, even when made of tungsten carbide, is subject to deformation, which causes an error that increases progressively toward high hardness levels. At the same time the hemispherical impression becomes small and too vague in outline for precise measurement or the indented area of a brittle metal may crack during the test. Beyond a Brinell Hardness Number of 700 the technique is not recommended.

The Rockwell conical diamond indenter is less subject to deformation errors, because of its great strength, and the dial gage readings of hardness are not plagued with optical limitations. Nevertheless, the stresses at the indenter tip when forced against very hard materials are very high. They tend to shatter, crack, or chip brittle test specimens and thus cause spurious values. The diamond tip itself may shatter against tungsten carbide, thus increasing experimental difficulties.

Both of these methods indent rather large areas of the test specimen, rendering them ineffective for checking

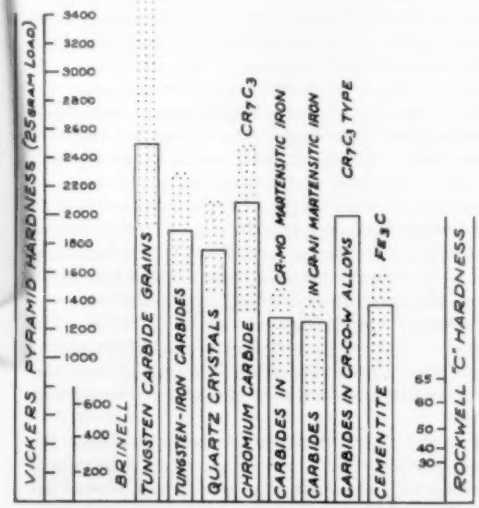
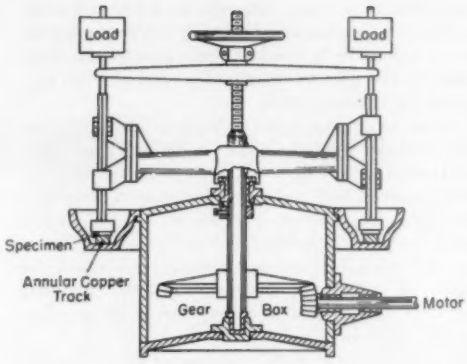


Fig. 2 Relative hardness of carbides in weld deposits



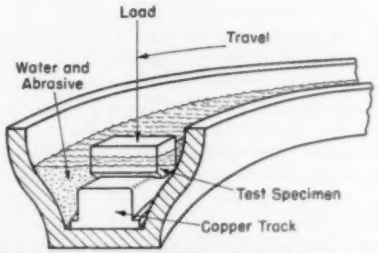


Fig. 3 Schematic sectional views of American Brake Shoe Co. high-stress abrasion test apparatus

the microscopic constituents of a heterogeneous structure. For this reason the Vickers Diamond Pyramid indenter, used with a light load to avoid specimen cracking, was selected for much of the hardness testing reported here. (Figure 2.)

Unfortunately, the optical difficulties incident to measuring the diagonals of tiny impressions from these indenters are a severe limitation on precision. When these are coupled with variations in hardness of a single constituent because of segregation, orientation, and other factors, the experimental error becomes large. To cope with this a practice of taking nine readings to represent each constituent was adopted. The median, mean and range are reported. Of course, when obvious limitations such as lopsided or cracked impressions are encountered, the questionable indentation is not included in the record. In most cases the standard deviations of the data groups were calculated and reported to provide an estimate of the reliability of the data.

Hot hardness is of interest because some wear situations normally involve high temperatures and others produce heat by friction at the wear face. The values over the range from 600 to 1200° F. were obtained by Rockwell testing, following procedures described in a previous paper.[1]

A standardized and validated abrasion test was available for this work, providing interesting comparative data. This wear measuring device, which was first described by Blake in 1928,[2] has proved very useful

over a period of years in the study of abrasion resistance. More recently the excellent ball mill field tests of Norman and Loeb[3] have provided a validation of its ranking ability in field service.[4]

The essential parts of the test apparatus are a lap, a trough for abrasive, arms for holding the specimens and applying uniform and measured pressure, a hydraulic classification system for removing fines and mechanism for dragging the test specimen over the lap, which is a flat circular copper track about 2 in. wide and 4 ft. in diameter. The standard abrasive consists of 40 lb. of quartz sand with a grain fineness number of 50, mixed with 30 quarts of water. Test pieces with a wearing face $1^1/_2$ x $2^1/_4$ in. are held against the lap with dead weight loading that produces from 50–60 psi. nominal unit stress on the abraded face. (Spring loading has been used in the past, but the current dead weight system is considered superior). In operation the specimens are moved over the track at a rate of about 119 ft. per minute for a total distance of 1.35 miles. (Fig. 3.)

Before each test, the specimen under investigation is worn in against the track until it has completely conformed to the minor irregularities in surface contour. For specimens containing tungsten carbide it is necessary to use granulated silicon carbide as the abrasive during the break-in period. The resulting specimen surface is relatively smooth since the silicon carbide is harder than the tungsten carbide and can effectively abrade it.

After breaking in, the specimens are carefully cleaned, dried and weighed. Then the test proper is begun on two specimens, the second of which is of annealed S.A.E. 1020 steel. Inclusion of this readily duplicated material provides a standard that will indicate any serious departure from the correct testing conditions. The final report of wear is given in terms of weight loss, in comparison with the standard, thus reducing the data for many materials to a common denominator. The relative ranking of several interesting engineering alloys and metallographic structures appears in Table 2.[5]

Table 2—Typical Wet Quartz Sand Abrasion Factors

American Brake Shoe Co. Laboratory Apparatus

Material	*Hardness, Bhn.*	*Abrasion, factor*
Ferrite (ingot iron)	90	1.40
Gray cast irons	200 ±	1.00–1.50
S.A.E. 1020 steel (annealed)*	107	1.00
White cast irons (pearlitic)	400 ±	0.90–1.00
Alloy white cast irons (pearlitic)	400–600	0.70–1.00
Pearlite (0.85% C steel)	220–350	0.75–0.85
Austenite (12% Mn steel)	200	0.75–0.85
Bainite	512	0.75
Martensite	715	0.60
Martensitic cast iron (ABK-metal)	550–750	0.25–0.60
Cemented tungsten carbide	—	0.17

* This is the standard reference material of the test. It loses about 15 gm. per hour when the specimen area of 2.6 sq. in. is abraded by No. 50 (A.F.A. grain fineness number) sand. Weight losses of the other materials are in proportion to the abrasion factors: thus ferrite would lose about 1.4 x 15 gm. or 21 gm. per hour. In practice the abrasion factor is derived from the weight loss divided by the precise weight loss of the companion 1020 steel standard.

Any engineering test depends for its usefulness on three factors: its reliability, its ability to rank different materials and its correlation with actual service performance, which establishes its validity for predicting behavior in the field. In a wear test especially, these factors should receive close scrutiny.

The present standardized procedure provides excellent reproducibility, as evidenced by duplicate tests. Statistically, the standard error of the test was calculated as 0.028 and of the average of two tests as 0.02. This implies that 68% of the determinations should fall within ±0.028 (±1.0 standard error) or a range of 0.056; 95% within ±2.0 standard errors; and 99.7% within ±0.084 (±3.0 standard errors) or a range of 0.168. These correlations are based on uniform materials such as cast specimens. Heterogeneous weld deposits are obviously likely to produce a greater scatter of values, but these do not reflect on the reliability of the test.

As an example of the relation between the statistical probabilities stated above and practical experience, the testing of austenitic manganese steel is pertinent. Because of an important company interest this alloy has been tested dozens of times over a period of many years. A value outside the range of 0.75–0.85 is very rare. This agrees well with an average value of 0.80 and a variation of ±2 S.E. or ±0.056 about this average, which theoretically should include 95% of the data.

There is some evidence that the expected error is proportional to or a percentage of the average factor. Thus at 0.50 factor a drop to 0.45 indicates a 10% improvement in wear resistance. At 0.25 a drop to 0.225 represents the same percentage. Applied to errors, a ±5% range is ±0.04 at the 0.80 factor level, while ±5% is ±0.01 at an average level of 0.20. This is not clearly

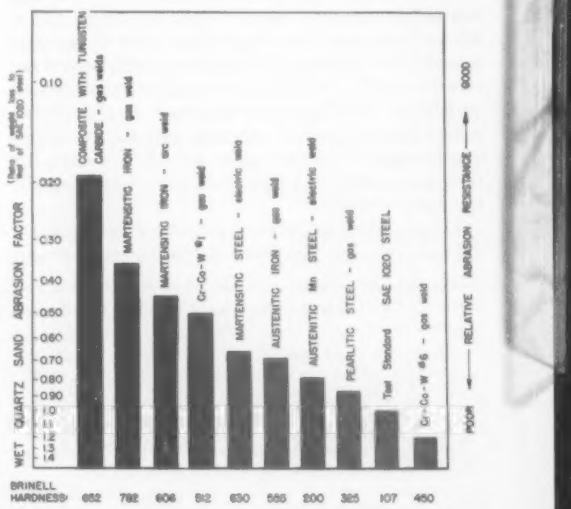

WET QUARTZ SAND ABRASION FACTOR (Loss of weight, gm., by test of SAE 1020 steel)
0.10
0.20
0.30
0.40
0.50
0.60
0.70
0.80
0.90
1.0
1.5
2.0
COMPOSITE WITH TUNGSTEN CARBIDE - gas welds
MARTENSITIC IRON - gas weld
MARTENSITIC IRON - arc weld
Cr-Co-W #1 - gas weld
MARTENSITIC STEEL - electric weld
AUSTENITIC IRON - gas weld
AUSTENITIC Mn STEEL - electric weld
PEARLITIC STEEL - gas weld
Tool Standard SAE 1020 STEEL
Cr-Co-W #6 - gas weld
POOR ← RELATIVE ABRASION RESISTANCE → GOOD
BRINELL HARDNESS 652 782 606 512 630 585 200 385 107 450

Fig. 4 Relative abrasion resistance of hard-surfacing deposits as determined with The American Brake Shoe Co. Laboratory Test

established, chiefly because the materials that can exhibit 0.20 factors are rarely homogeneous, but it is mentioned here to aid interpretation of the data. Tungsten carbide welds may fall in the lower range.

The next requisite of a good test, ranking ability, is demonstrated by Fig. 4, which applies to a group of hard-surfacing materials. Some wear tests, though reliable in the sense that duplicate tests are nicely reproducible, show so little difference between reputedly good and poor materials that the data cannot be interpreted usefully. This wet sand abrasion technique does not have such a limitation, as it has shown at least a 1:10 difference in weight loss for excellent vs. poor alloys.

The final factor, validity established by service correlation, is the most difficult to confirm. This is due primarily to the variability and consequent lack of reliability that plagues field tests. Replications are sometimes almost impossible to obtain and yet when they are available they frequently show wide scatter bands that point to large probable errors.

One of the best field wear studies available has been reported by Norman and Loeb[3] from several years of work with the Climax Molybdenum Company's milling operations. Fortunately it was possible to provide several lots of materials for this field program from the American Brake Shoe Co. Metallurgical Laboratory and at the same time retain duplicate specimens for laboratory tests on the apparatus described here. The wear test data from this machine were obtained in a few weeks; the matching service data required several years. The correlation is shown in Fig. 5.[4] Obviously there is a useful relationship.

A disconcerting limitation of wear test correlations is their specific nature. In this case an important question relates to the range of conditions outside of ball milling for which the test data are applicable. It is known from experience that abrasiveness of material handled, the fineness of the abrasive particles and the stress available for crushing the abrasive are important variables. When these do not depart much from ball milling practice, useful predictions are possible. Where one or more factors are quite different, like the low stress that operates on plow share faces, a valid correlation is not expected.

These remarks are included as cautions relating to the data herein. For this paper the field correlations are

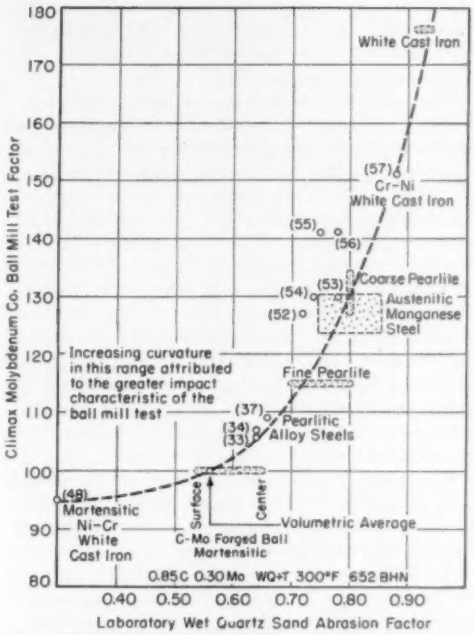


Fig. 5 Correlation of Climax ball wear tests and American Brake Shoe Co. Laboratory abrasion tests

(Numbers by data points refer to items in A.I.M.E. T.P. 2319, Table 4.)

of limited significance since the chief purpose is to point out the amount and kind of variability that occurs in weld deposits. Thus the reliability of this abrasion test is here of primary importance.

GRANULE SIZE

Some accumulated data were available to help answer the question: does granule size influence abrasion resistance? From files of laboratory tests covering work during the past five years, abrasion factors were tabulated for gas welds of several grades and for arc welds with 40/120 mesh granules. All of these results were from commercial materials (excluding many experimental items in the file) made by three manufacturers. The pertinent tests are summarized statistically in Table 3.

Table 3—Wet Sand Abrasion Resistance* of Composite Tungsten Carbide Weld Deposits

	Oxyacetylene welds			*Arc welds*
Nominal Granule size	20/30	30/40	40/120	40/120
Average ($\bar{x}$)	0.187	0.166	0.184	0.63
No. of tests (n)	16	13	7	8
Range (R)	0.09	0.196	0.11	0.28
Variance (σ^2)	0.00078	0.00257	0.0017	0.0097
Standard deviation (σ)	0.028	0.0505	0.0417	0.0982
Estimated universe standard deviation (s)			0.0449	0.105
Standard error of mean ($S\bar{x}$)			0.017	0.037
	Scatter bands			
$\bar{x} \pm$ 1 standard deviation	0.16/0.22	0.12/0.22	0.14/0.23	0.53/0.73
$\bar{x} \pm$ 2 standard deviation	0.13/0.24	0.07/0.27	0.10/0.27	0.43/0.83

* The values in the table are wet sand abrasion factors (as graphed in Figs. 4 and 8) or various statistics calculated from them.

For gas welds, the average abrasion factors of 0.187 and 0.184, for 20/30 and 40/120 granule sizes,* are obviously so close together that the difference is insignificant. The difference of 0.187 − 0.166 = 0.021 for the 20/30 and 30/40 sizes is somewhat greater, but application of a statistical test to the difference of the two means[6] indicates that there is almost a 20% probability that the observed difference is due to chance, and, therefore, cannot be considered significant.

The data from Table 3, therefore, lead to the conclusion that, with this test technique, 95% of gas weld abrasion factors will fall within a scatter band extending approximately from 0.07 to 0.27, and averaging near 0.18, regardless of the carbide granule size. This average value is better than any corresponding factor from other types of weld deposits encountered during years of testing.

This scatter band can be narrowed by rigorous control of the tube rods and of welding technique, but the results are likely to be of only academic interest to the user of hard facing. It is more practical to demonstrate the operation of several other factors.

Two aspects justify further comment. The original abraded face is relatively smooth because the silicon carbide break-in abrasive produces negligible differential wear. However, quartz sand (which is selected because quartz is the commonest abrasive occurring in nature) is softer than tungsten carbide though it can be harder than the matrix structures. The sand test run, therefore, produces a rough surface with primary carbide granules standing in relief.

This differential wear also occurs in field service and is an important selection factor. If a very smooth surface is necessary, the rough wear pattern may render this hard facing type unsuitable. If some roughness is tolerable, the finer granules are selected, and if extreme roughness is wanted, the coarse grades are favored. Examples of the last are the application to horseshoes for nonskid qualities, or for cutting qualities as on rotary drill bits and scarifier teeth.

The relief pattern also can affect the wear test results. If welding technique has left a layer of granules close to the base metal junction, where they will tend to collect by gravity if the melt is fluid enough, the upper layer of the deposit may be matrix rich and granule poor. More wear in the test and a higher factor would then be expected. However, if the welder has carefully left most of the granules at or near the surface, a better than average initial factor could be expected. Such variables can account for some of the scatter indicated by the statistics of Table 3.

This idea was experimentally confirmed by successive tests on the same abraded face, with a break-in period preceding only the first test. Successive factors usually decrease, reaching values around 0.09 eventually. It should be recognized that generally the factors reported herein apply to standard tests that begin with a smooth face, and are characteristic of the matrix-granule combination. Where carbides standing in relief at the beginning of a test take the brunt of abrasion, the resulting factors are so designated.

The 40/120 mesh granules are coarse in comparison with tungsten carbide powder, which may be included in the filler. Some solution of the granules takes place during gas welding with coarse granules; with fine powder much more solution could be expected. In the extreme case where all the carbide is dissolved, the deposits could have been made from cast rods of the tungsten iron type as well as from tube rod. While outside the scope of this paper, it is considered worth while to point out that if such a deposit is acceptable, it is better to produce it with a cast rod because of the probability of greater homogeneity.

WELDING METHOD

High arc temperatures encourage solution of the tungsten carbide filler. This in turn affects abrasion resistance significantly. Since small granules dissolve readily, the effect can be demonstrated strikingly by comparing gas and arc welds in Table 3.*

To provide further insight into the influence of both granule size and welding method, a group of specimens was selected from a large series representing a study of granule-size combinations. These were abrasion tested, sectioned, examined microscopically and photographed to provide a graphic rather than a statistical exhibit. Several metallurgical features of such composite tungsten carbide deposits were illuminated by these studies, which are detailed below.

This test group consists of twelve specimens, six prepared by gas and six by arc welding on $1^1/_2$- x $2^1/_4$- x 1-in. thick blocks of S.A.E. 1020 mild steel. The deposits, made with $^3/_{16}$-in. diameter tube rod, were about $^1/_8$ in. thick and required two passes to cover the top of the block with a single layer.

Oxyacetylene welds were made with a reducing flame, which for hard facing usually implies a feather three times the length of the inner cone. The base blocks were preheated to about 1200° F. No flux was employed.

Electric welds were made with bare rods† since a variety of complicating effects can be introduced by flux coatings. This should be remembered when interpreting the results. A nominal current of 160 amp, straight polarity, and an arc-voltage range of 26–30 were used. The blocks were not preheated or fluxed.

The composite tube rods were filled with cast tungsten carbide, crushed and sized as indicated below. The filler comprised 60% by weight of the rods, the containing tubes of mild steel contributing 40%.

* U. S. Standard Sieve Series screen sizes are used for these classifications. The symbol 40/120 indicates that all of the granules will pass through a 40 mesh screen but would be retained on a 120 mesh sieve.

* The difference between the averages is 0.63–0.184 = 0.446; the estimated standard error of this difference between means is 0.0154; $t = \frac{0.446}{0.0154} = 29.1$, which with 13 degrees of freedom provides $P < 0.001$. The difference is thus highly significant.[6]

† In contrast with many hard-facing materials, bare tube rods of this type are widely used for arc welding.

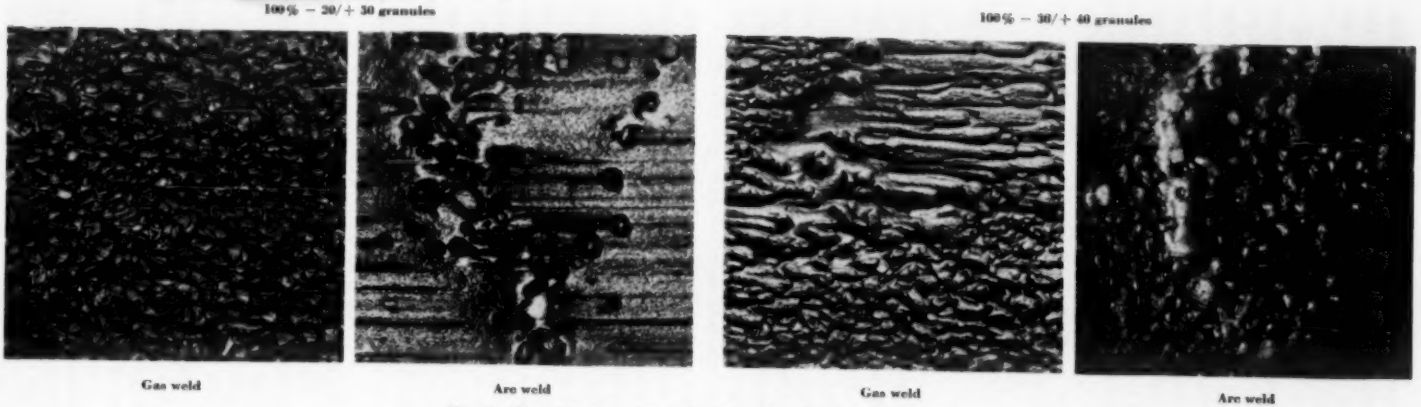


Fig. 6 Magnification views of wet-sand abraded surfaces. 5 ×

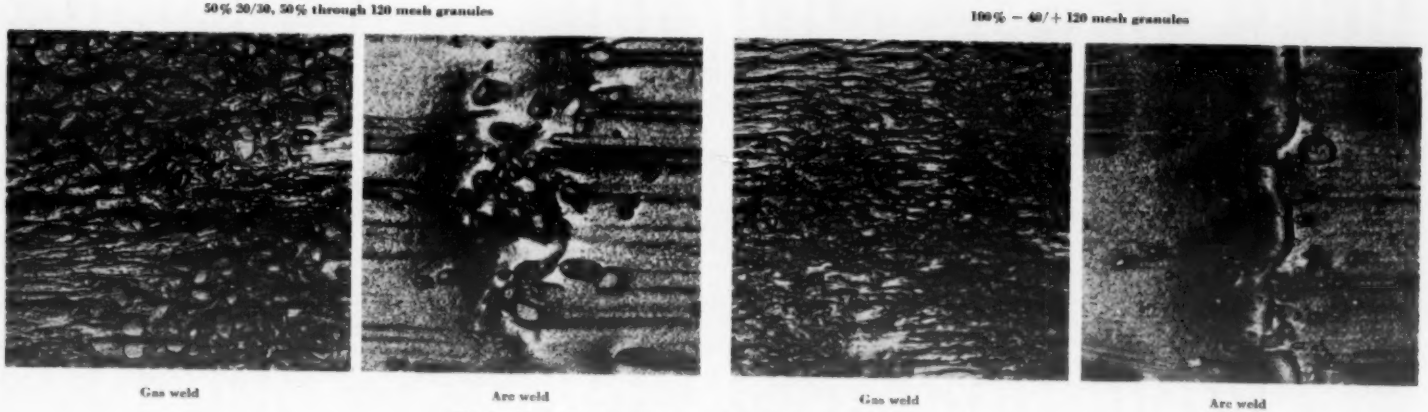


Fig. 7 Magnification views of wet-sand abraded surfaces. 5 ×

Six filler variations were included, typed as:

I. 100% −20/30 (through 20 mesh but held on 30 mesh)
II. 50% −20/30 : 50% finer than 120 mesh
III. 100% −30/40 (through 30 mesh but held on 40 mesh)
IV. 50% −30/40 : 50% finer than 120 mesh
V. 100% −40/120 (through 40 mesh but held on 120 mesh)
VI. 68% −40/140 : 32% finer than 140 mesh

After breaking in as described under experimental procedure and after wet sand abrasion testing the rough abraded faces appeared as in Figs. 6 and 7. The gas welds were subjected to three test periods without interposed break ins. Thus the successive abrasion factors (W.S.A.F.) tend to decrease as the projecting carbide granules take the brunt of abrasion. The arc welds were carried through only one test period each.

Two visual differences associated with welding method are apparent. The carbide granules are more uniformly distributed in the gas welds and they are present in greater quantity. Distribution is controlled by the skill of the welder, but electric deposition is more difficult to control All of these specimens were welded by one individual.

For comparison, the wear resistance in relation to the salient variables is shown in the chart of Fig. 8 for ten specimens. There is a hint that inclusion of 50% fines with coarser granules slightly improves the initial abrasion resistance of the gas weld matrix. This is consistent with easier and thus greater carbide solution contributing to a harder matrix. However, note that all of the initial gas weld factors for a given granule size fall within plus or minus 1.0 standard deviation scatter bands (Table 3) and slight apparent superiority cannot be accepted as significant without further proof.

There is a clearer case for granule size control of arc welds, which exhibit a drop in abrasion resistance when 50% or more of granules below 40 mesh is included (Fig. 8). With 100% of coarse granules (20/30 or 30/40 sizes) the arc welds, with factors of 0.41 and 0.40, are better than the ±2 standard deviation scatter band (Table 3) for the 40/120 arc type. The poorest abrasion resistance of the entire series (0.83) was shown by the 40/120 granule welds that contained 32% of fines below 140 mesh size. These indications are considered significant.

The size distribution of filler granules is subject to control in manufacture and to specification for important applications. However, the power in the arc is under the welder's control through both the machine setting and the length of the arc he maintains. Both granule size and arc power are in the same category since both affect the amount of carbide that is dissolved. The wide range in performance that may stem from these is worthy of serious consideration by users of hard surfacing.

In the magnified views of Figs. 6 and 7 the relief effect of the wear pattern and the relative granule density in gas vs. arc welds are clearly shown. These pictures were taken after the first standard abrasion test and represent the status after one hour of abrasion on an originally smooth face.

Cross sections of some of these welds appear at 10 X magnification in Fig. 9. Note that the gas welds have a straight weld junction with practically no underbead fusion. There are shallow zones of pearlite, showing in Fig. 9 as dark bands below the junctions, that result from carbon diffusion into mild-steel base. The arc welds, in contrast, melt considerable of the base metal, leaving a wavy contact area and diluting the weld deposit. The 60% granule : 40% steel ratio thus no longer holds in an arc-weld deposit. The amount of penetration, or base fusion, is a variable that depends considerably on the welder.

MATRIX CHARACTER

Matrix study of the composite weld deposits is essentially concerned with tungsten steels and irons. Gregg[8] has summarized the early work on such materials, which

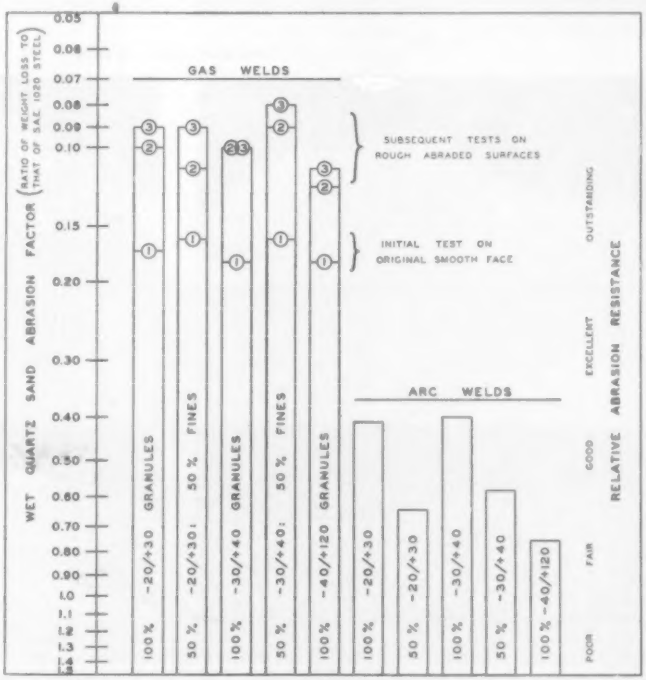


Fig. 8 Abrasion resistance of composite tungsten carbide weld deposits as affected by granule size and welding method

indicates several departures from the familiar pattern of iron-carbon alloys. Carbide solubility in austenite decreases as tungsten content increases. Iron tungstide may form, and a double carbide, $(Fe, W)_6C$, is expected.

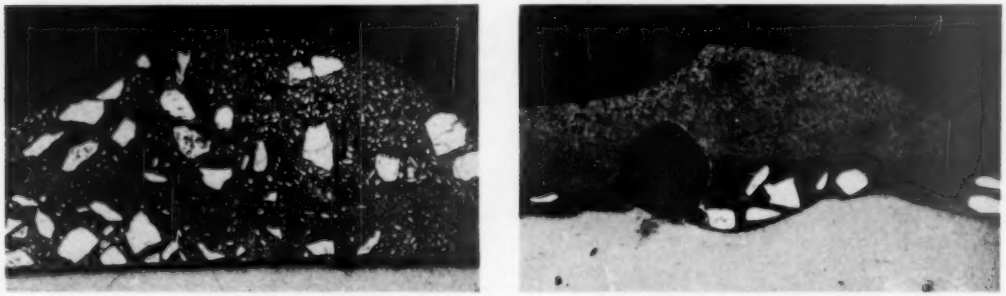
In the high carbon alloys the ternary system of iron, tungsten and cementite is of interest. Between cementite (Fe_3C) and tungsten a complex pseudobinary system is characterized by a theta (θ) phase (Fe_3C containing up to 8% tungsten), a eutectic with about 32% tungsten that melts near 2190° F., and an eta (η) phase between 65 and 85% tungsten.* The eutectic temperature falls from 2190° F. to the ternary eutectic at 1985° F., 3.7% C and 17% W, as the proportion of iron increases. It then rises to the 4.3% C., 2065° F. eutectic of the iron-carbon system.

The eta phase (η) is the double carbide, $(Fe, W)_6C$, which has a variable composition. It may range from 1.64 to 1.99% carbon and probably is basically $(Fe_4W_2)C$ or $(Fe_3W_3)C$.

Composite weld deposits from 60% carbide: 40% tube rods, provided all of the tungsten carbide were dissolved and oxidation or dilution effects were negligible, should consist of mixtures of θ, η and γ, or tungsten-rich cementite, the double carbide and austenite. Much of the eta and all of the theta phase should appear in a eutectic structure, associated with small amounts of tungsten-rich austenite. Dilution with iron from base metal fusion is expected to increase the austenite proportion at the expense of the carbides.

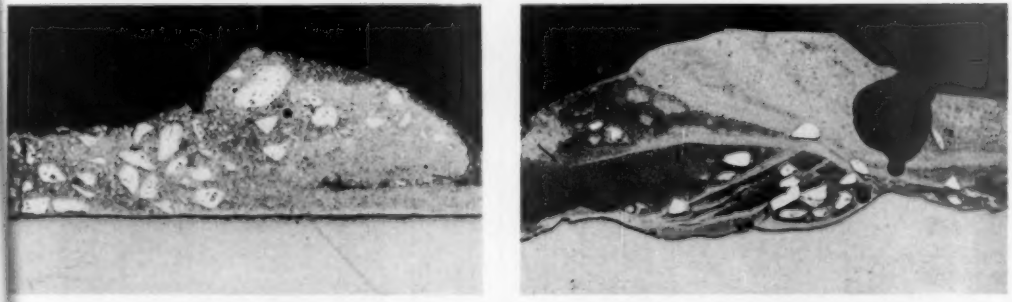
* These phase designations follow those of Takeda, as condensed by Gregg[4].

50% 20/30; 50% − 120 mesh



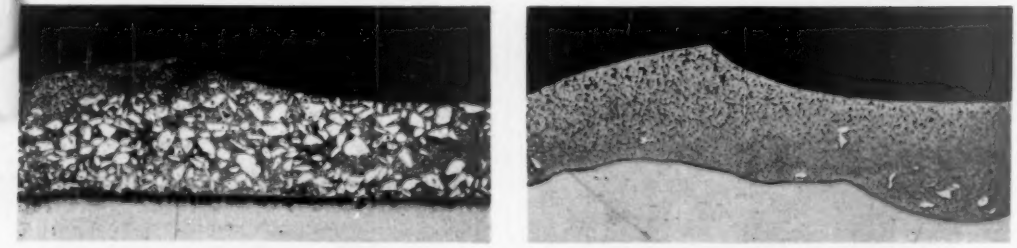
Gas weld — Arc weld

50% 30/40; 50% − 120 mesh



Gas weld — Arc weld

100% 40/120 mesh granules



Gas weld — Arc weld

Fig. 9 Low Magnification cross sections through overlays. 10 ×

Another eutectic, between iron tungstide (Fe_3W_2) and iron occurs at 33% tungsten[7] and 2770° F. This is little below the melting point of iron, but as carbon is added the eutectic temperature decreases toward 2435° F. (1.4% C, 27% W) and thence to the ternary eutectic at 1985° F. It thus appears likely that eutectiferous structures will be frequently encountered in tungsten-carbide weld deposits.

In the red heat range tungsten-rich austenite is associated with the carbides and eutectics. This austenite usually transforms on cooling to the aggregate constituents: bainite, pearlite or martensite.

Like many other alloying elements, tungsten depresses the temperature of martensite formation. Greninger and Troiano[9] presented a good description of the general behavior, which was later amplified to permit estimating the temperature (Ms) at which martensite begins to form, the percentage that is expected at a given temperature below this, and the temperature at which the transformation is substantially complete. Payson and Savage[10] contributed specific data on three tungsten steels, which are reproduced for convenience in Table 4. From their data it may be concluded that tungsten lowered the start of martensite formation about 10° F. for each per cent in the steel.

Table 4—The Effects of Tungsten on 0.50% C Steels after Payson and Savage[10]

Type	*C %*	*Mn %*	*Si %*	*W %*
1 Mn	0.47	1.02	0.34	——
1 Si	0.47	0.40	1.06	——
1 W	0.53	0.38	0.32	1.04
3 W	0.45	0.34	0.27	3.42
5 W	0.46	0.37	0.27	4.78

	Austenitizing —Treatment—		*Tempering treatment in quench-temper —procedure—*		*Martensite, formation ° F. (approx)*	
Type	*° F.*	*Min.*	*° F.*	*Sec.*	*Begins*	*Ends*
1 Mn	1800	10	600	5	590	200
1 Si	1800	15	625	3	610	275
1 W	2300	3	1200	20	625	300
3 W	2325	3	1200	30	600	300
5 W	2325	3	1200	30	590	275

Carbon is much more potent than tungsten in lowering the martensite formation temperature, and thus is expected to control the resulting residual stresses. However, martensite cannot form from austenite that has previously transformed to pearlite or bainite. Tungsten here plays an important role in making these reactions so sluggish that they may be largely or entirely suppressed at weld cooling rates. In such cases the welds are self or air hardening because of the austenite to martensite reaction.

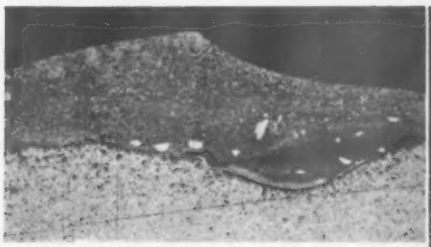
Martensitic structures are expected to be more abrasion resistant than austenite, pearlite or bainite, providing wet sand abrasion factors from 0.60 to 0.85. When martensite is combined with massive carbides a definite improvement in wear resistance is expected. Such martensitic irons are popular hard-facing (Fig. 4) and casting alloys.[6, 11, 1] Some matrices of the weld deposits described here are similar in nature.

The tungsten bearing carbides are less soluble than iron carbide and may not let tungsten and carbon exert their full effect on transformation if they have been precipitated by holding below or by slow cooling from the freezing point. This is very pertinent to weld deposit behavior since slow vs. fast cooling, as in gas vs. arc welding, may establish a marked difference in matrix composition and, therefore, in transformation on cooling.

Residual stresses are likely to be high when martensite forms at low temperatures. Since both tungsten and carbon (the latter very powerfully) depress the Ms point, the tendency toward such stresses or their sequel, weld cracking, increases as carbon and tungsten are picked up during welding.

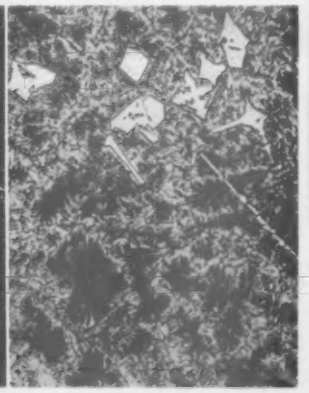
These interrelationships could be defined quantitatively by careful experimentation, but since the thermal and mechanical variables of welding are usually very large, practically it should suffice to recognize trends and to use them in aiming at the end result desired. Clarification of trends is one purpose of this paper.

Figure 10 exemplifies one extreme condition. Arc



Arc weld cross section. 10 ×

Carbide distribution. 50 ×



Eta carbide $(Fe,W)_6C$ crystals in eutectiferous matrix. 250 ×

Fig. 10 Arc-weld deposit from rod containing 68% 40/120 granules and 32% fines below 140 mesh. Most of the primary tungsten carbides have been dissolved

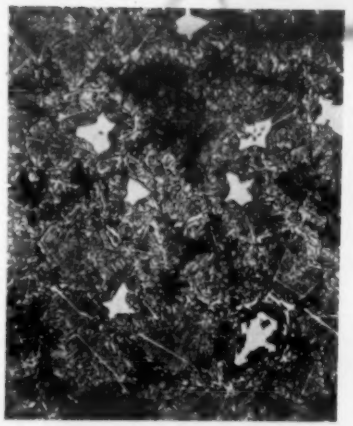
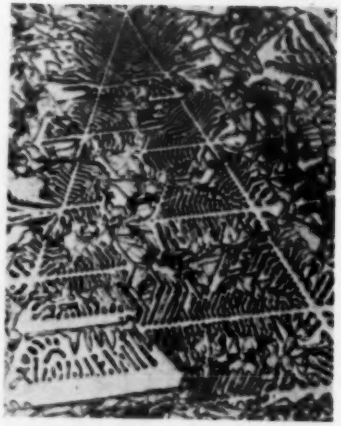


Fig. 11 Arc weld from tube rod containing 100% 40/120 mesh granules, showing eta carbide crystals and a eutectiferous structure. 250 ×

A. 500 ×

B. 1000 ×

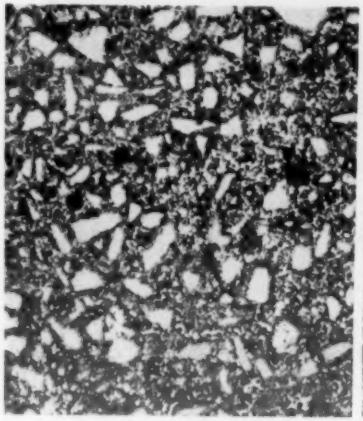
Fig. 12 The eta phase double carbide formed around the border of a primary tungsten carbide granule (A), and a higher magnification view of part of this field showing martensitic areas between the eta crystals and the eutectic pattern



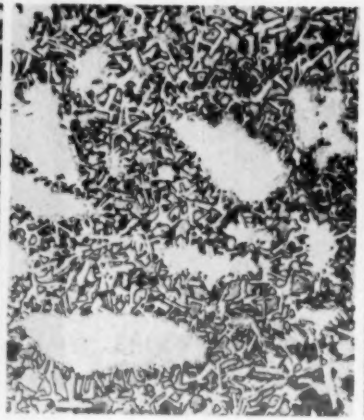
The chief constituents here are the eta phase, $(Fe,W)_3C$, and austenite that has partially transformed to martensite. From an arc weld with 100% 40/120 mesh granules. 1000 ×

Fig. 13 Eutectic in the ternary system iron–tungsten–cementite

Gas weld from 50% 20/30 : 50% – 120 granule tube rods

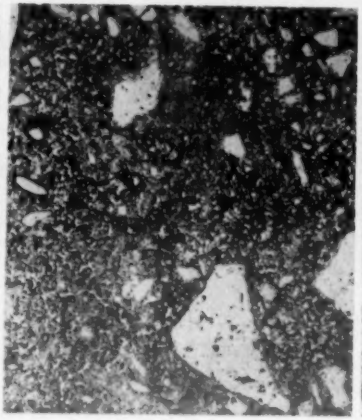


A. 50 ×

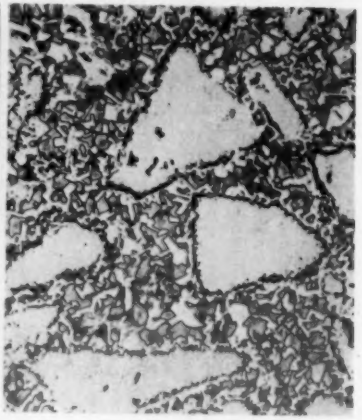


B. 250 ×

Gas weld from 50% 30/40 : 50% – 120 granule tube rods



C. 50 ×



D. 250 ×

Figure 14

welding burns out carbon and at the same time creates high temperatures favoring quick solution of the primary tungsten carbide granules. In this case they were all under 40 mesh in size, which also encouraged rapid solution. The end result is a dark etching matrix like tungsten steel, containing a very few undissolved tungsten carbide particles and a few scattered crystals of the secondary carbide tentatively identified as the Fe_4W_2C type. A cluster of these carbide crystals also appears at 250 × which reveals a eutectiferous pattern in the matrix. This eutectic dominates the structure at higher magnification and is interspersed with areas that appear martensitic.

The companion specimen, but with 100% of the granules between 40 and 120 mesh, has a similar abrasion factor (0.75 vs. 0.83) and a quite similar structure (Fig. 11). The dark patches in Fig. 11 are possibly very fine pearlite beyond the resolving limits of the optical equipment. (Low magnification pictures of this weld also appear in Fig. 9 (A).

The relation of the secondary carbide to the primary granules is shown in Fig. 12. The reaction between iron and tungsten carbide produces the eta phase, with further iron dilution trending the structure toward a greater proportion of austenite (or its transformation products) and the eutectiferous structures that provide the striking geometry in Fig. 13.

Quite a different pattern is developed by gas welding. If coarse granules alone are used carbide solution is at a minimum and the matrix will be relatively low in tungsten. The reducing flame customarily used for hard facing will add carbon, however, and the matrix produced will be higher in carbon but lower in tungsten than that of arc welds. Inclusion of fines along with coarse granules may permit solution of more tungsten, but will also provide additional carbon. The same phases: tungsten bearing cementite, the double carbide and austenite (or its products), are expected in both gas and arc welds but their compositions as well as proportions are quite different.

The most obvious difference between arc and gas weld is the greater volume of secondary carbides in the latter, especially when fines are included in the granule quota. Figure 14 shows two welds at two magnifications. The rods contained 50% of the granules with the nominal coarse particle size indicated and the remaining 50% as fines below 120 mesh. Low magnification (10 ×) pictures showing the coarse and fine granule distribution are included in Fig. 9. The matrix here consists chiefly of secondary carbides and transformed austenite. The large proportion of iron rich carbide suggests that the matrix is relatively brittle in comparison with those from electric welds (Fig. 10) or those of gas welds in Fig. 15, from corresponding rods that do not include fines. Similar matrix structures were produced by arc welding 40/120 rods (Fig. 11—250 ×) and gas welding 20/30 and 30/40 rods without fines (Fig. 15—250 ×).

The Fig. 15 structures should contain less tungsten and carbon than those of Fig. 14 and are probably tougher. During this study it was not economically feasible to sample the matrix for chemical analysis and for tests other than hardness, which accounts for the qualified wording of statements like those above. Even if precise data were available it should be remembered that such qualifications would be necessary when welds made under other circumstances are considered.

Maximum solution of primary granules during gas welding would be expected with the rod containing the largest proportion of fines. This is illustrated in Fig. 16 (*A*), (*B*), (*C*), (*D*) and (*E*) for the group of this study. The rod contained 68% of the filler in the 40/140 mesh range and 32% below 140 mesh. Gas welding was apparently conducted near the minimum temperature possible since there is evidence of bits of undissolved sheath (Fig. 16 (*B*)) in the weld.

Large amounts of the eta double carbide are present in the matrix, associated with martensite. The contribution of a granule of primary tungsten carbide to its envelope of the eta carbide is well shown in Fig. 16 (*D*). The darker etching background shows the acicular pattern of austenite that has transformed to bainite or martensite (Fig. 16 (*E*)).

HARDNESS

On structures as heterogeneous as these, it is generally misleading to quote Rockwell or Brinell values, since they ordinarily are some fortuitous average of the several constituents. This confusing circumstance is expected to be at a minimum where the carbide granules are of minimum size. Consequently such fine granule samples were selected for hot hardness tests, this technique being available only with a Rockwell instrument. For the coarser granule material the Vickers Diamond Pyramid with a 25-gm. load was used in an attempt to get significant hardnesses on the individual micro constituents.

Micro hardness determinations on very hard compounds involve a rather large experimental error. To gain an estimate of this, a single granule of cast tungsten carbide was selected in each of four cases and nine readings taken on it. The data appear in Table 5.

By inspection of the average median (2392) and average mean (2411) values, we are led to the conclusion that the Vpn. of cast tungsten carbide is about 2400. From the four standard deviations we have an average standard deviation of 146± a standard error of 72* The statistical implication is that other samples of the standard deviation, similar to the four we have obtained, will be within the range 146 ± 72 or 78 to 218 about 68% of the time. With error of this magnitude possible, it cannot be reasonably concluded that the four means are significantly different from the average mean of 2411. The apparent differences could easily be due to chance variation of the hardness determination experimental errors.

* This standard error is computed as the estimated universe standard deviation of the mean standard deviation.

Similar reasoning will discount the differences among the median values, which are reported because they are less likely to be seriously affected by unusually divergent maximum or minimum observations.

From the computed ranges that should be required to cover 99.7% of the expected scatter band (mean ±3 standard deviations) for the most erratic sample we may conclude that individual hardness values might scatter from 1796 to 3295 Vpn. Using round numbers then, from this small sample survey we think that cast tungsten carbide may give individual hardness values from 1800 to 3300 Vpn. because of experimental error, while its actual hardness is near 2400 Vpn. as determined by this Tukon instrument. The testing equipment was gone over carefully before this work and should be reliable, but because recognized standards are lacking in this hardness range it is not possible to give assurance of absolute accuracy here.

A four specimen survey of tungsten carbide hardness, measuring the Vpn. of nine different granules in each specimen, appears in Table 6. The data, statistically considered, are not significantly different from those in Table 5, which represents only four granules, even though Table 6 covers 36 individual granules.

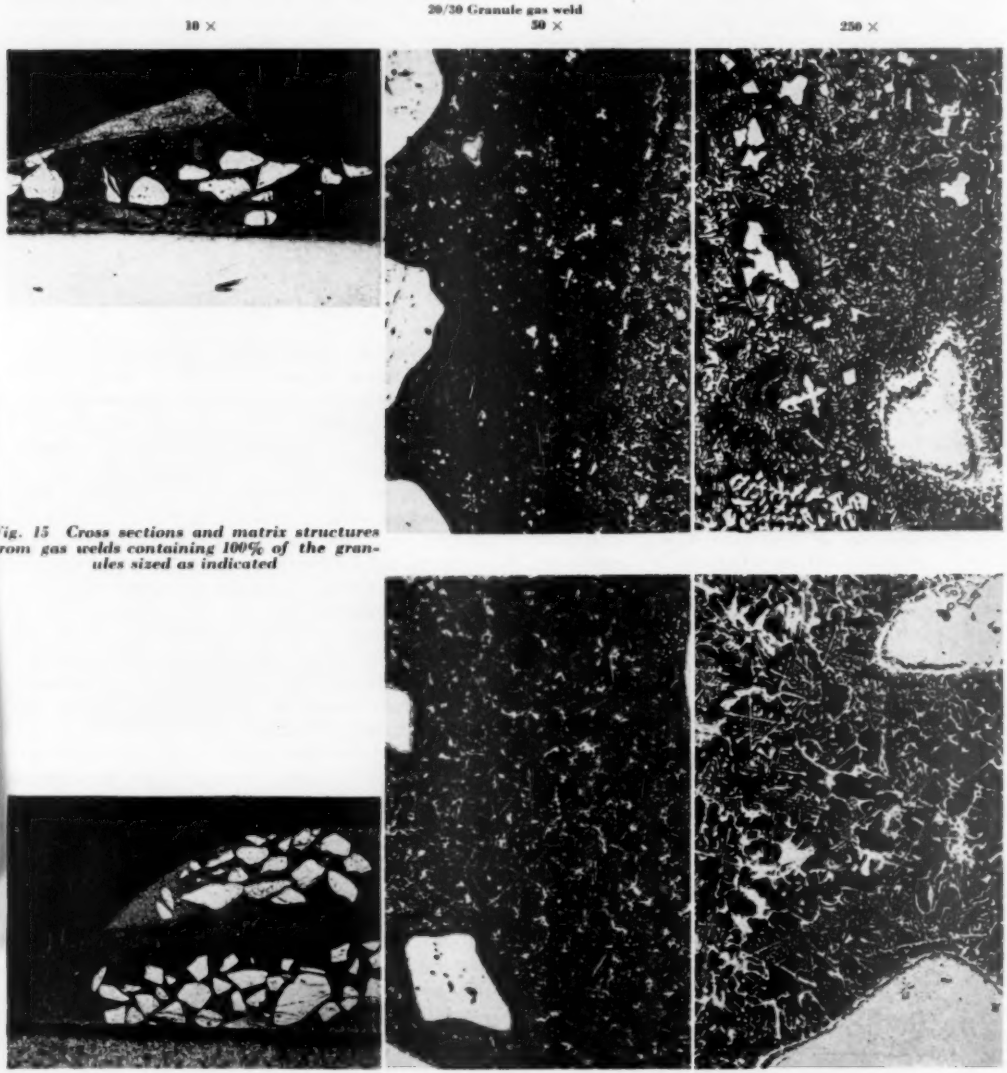


Fig. 15 Cross sections and matrix structures from gas welds containing 100% of the granules sized as indicated

Table 5—Scatter Band of Micro Hardness Values on Cast Tungsten Carbide Granules

Each of the Four Sets of Statistics Refers to a Single Granule, N = 9

		Vickers pyramid number—25-gm. load						
Micro No.	*Lab. No.*	*Min.*	*Median*	*Max.*	*Mean*	*Std. dev.**	*S†*	*Av. ± 3s Range*
S203	R2413	2175	2385	2477	2345	102	108	2053–2705
S203	R2413	2098	2297	2790	2377	226	240	1796–3295
S205	R2419	2276	2501	2938	2504	184	195	2010–3206
S205	R2419	2340	2385	2575	2418	71	75	2208–2658
Average:			2392		2411	146 ± 72		

* The sigma or standard deviation of the sample of 9 observations.

† The estimated standard deviation of the population or universe from which the sample of 9 values was taken.

The statistical computations leading to the values above were made on impression diagonal measurements, which is the physical quantity that is expected to vary normally. The last step in each calculation is conversion from the diagonal dimension to the Vickers Pyramid Number, which varies inversely with the square of the diagonal. For this reason the standard-deviation and S values are correct only at the average Vpn. level and multiples cannot be applied directly to calculate the ranges. Range calculations were made before converting diagonal measurement to Vpn. values.

Table 6—Scatter Band of Cast Tungsten Carbide Micro Hardness Values

Each of the Four Sets of Statistics Refers to a Test on Each of 9 Different Granules

		Vickers pyramid number—25-gm. load						
Micro No.	*Lab. No.*	*Min.*	*Median*	*Max.*	*Mean*	*Std. dev.**	*S†*	*Av. ± 3s Range*
S203	R2413	1924	2155	2430	2197	173	184	1735–2870
S203	R2413	2026	2385	2680	2392	190	201	1888–3129
S204	R2417	2362	2501	2627	2485	84	89	2238–2775
S204	R2417	2098	2430	2575	2397	143	152	2002–2923
Average:			2368		2368	148 ± 47		

* † See footnotes below Table 5.

The double carbide or eta phase was tested by ten groups of nine determinations each, representing eight of the specimens described under the section on Welding Method. Its identity was confirmed by comparing an X-ray diffraction pattern from the specimens in Figs. 16 and 9 (*C*) with that given by Goldschmidt[12] for high-speed steel carbide (Table 7). The small differences in lattice parameters are easily explained by the differences in chemical composition.

The statistics in Table 8 suggest that a Vpn. around 1745 is representative of the (Fe, W)$_6$C constituent. The scatter encountered indicates that a range of 913–3820, reflecting phase variability and chance experimental errors, might be required to bracket individual determinations. The observed range for 90 test indentations was from 1160 to 2526 Vpn.

The austenite and its transformation products in these structures may be expected to range from about

Table 7—Interplanar Spacings of Eta Phase Diffraction Patterns

	High-speed steel carbide after Goldschmidt[12]		*Eta phase in composite welds*			
			R2395		*R2397*	
hkl	*d*	*I*	*d*	*I*	*d*	*I*
400	2.75	M-S	2.76	...	2.734	...
334	2.52	M-S	2.51	...	2.516	...
422	2.249	S	2.25	...	2.245	...
333,511	2.122	VS	2.12	S	2.11	S
440	1.948	M	1.95	...	1.944	...
551,711	1.545	M-S	1.54	...	1.54	...
553,731	1.44 *	M-S	1.435*	...	1.432*	...
733	1.347*	M-S	1.35 *	...	1.347*	...
660,822	1.300*	S	1.30 *	...	1.299*	S
555,751	1.274*	M-S	1.28 *	S	1.285*	...
...	...	...	1.27	...	1.28 *	...

Intensity Code: V = very strong, S = strong, M = medium; R2395 is a gas weld with rod containing 68% 40/140; 32%—140; R2397 is a gas weld with rod containing 100% 40/120 granules.

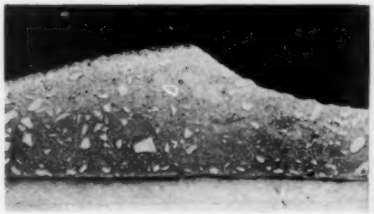
* Indicates lines possibly coincident with primary tungsten carbide lines, and thus not reliable for identification.

Table 8—Scatter Band of Eta Carbide, (Fe,W)$_6$C, Micro Hardness Values

Each of the Ten Sets of Statistics Refers to a Survey Involving Nine Tests, Each on a Single Particle, at Different Locations in the Specimen

		Vickers pyramid number—25-gm. load						
Micro No.	*Lab. No.*	*Min.*	*Median*	*Max.*	*Mean*	*s at Mean**	*Av. ± 3s range*	*Photo reference*
S202	R2395	1449	1815	2026	1761	211	1270–2605	Fig. 16
S203(a)	R2413	1729	1771	1924	1809	67	1623–2028	Fig. 15, 6
S203(b)	R2413	1257	1538	2009	1594	221	1097–2524	Fig. 15, 6
S204	R2417	1515	1991	2136	1901	239	1350–2873	Fig. 14, 9, 7
S205(a)	R2419	1207	1757	1991	1609	280	1018–2915	Fig. 15, 6
S205(b)	R2419	1329	1635	2026	1638	251	1089–2740	Fig. 15, 6
S206	R2425	1503	1830	2526	1880	332	1182–3443	Fig. 14, 9
S207	R2427	1688	1830	1974	1840	92	1592–2151	Fig. 10
S208	R2429	1248	1743	2175	1768	324	1096–3318	Fig. 11, 9, 7
S211	R2451	1160	1688	2255	1647	387	913–3820	Fig. 6
Average			1780		1745			

* The estimated standard deviation of the population or universe from which the sample of nine values was taken.

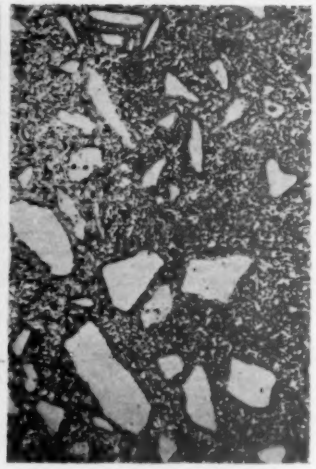


A. Weld cross section at 10 ×

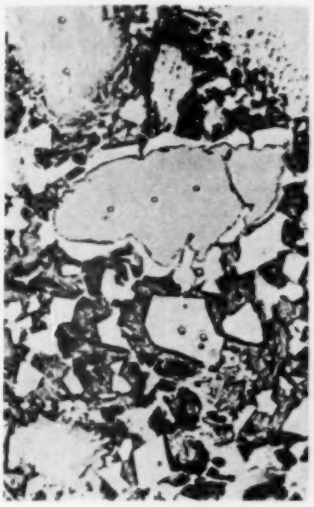
Fig. 16 Gas weld from tube rod containing 68% of the granules 40/140 mesh and 32% finer than 140 mesh

Higher magnification photomicrographs of a gas weld containing granules sized 68% to 40/140 mesh and 32% as fines below 140 mesh, and illustrating formation of the eta double carbide from a granule of primary tungsten carbide (D) as well as the transformed austenite in the background (E)

B. Wet sand abraded face at 5 ×



C. Carbide distribution at 50 ×



D. 500 ×

E. 1000 ×

300 Vpn. to perhaps 700 Vpn., depending on the kind and amount of transformation. A survey on the specimen shown in Fig. 15 gave erratic values averaging 1000 Vpn., but it is suspected that these are too high because of underlying and closely adjacent carbides.

The theta phase, or tungsten bearing cementite, likely occurs only in the eutectiferous structures. These yielded an average Vpn. of about 1000 with an observed range of 841–1096 for the specimen in Figs. 15 (*D*), (*E*), (*F*) and 13.

The hardness values plotted in Fig. 2 differ slightly from those just reported, since they are the result of previous work. The data detailed herein thus have the status of check determinations.

The statistical computations leading to the values above were made on impression diagonal measurements, which is the physical quantity that is expected to vary normally. The last step in each calculation is conversion from the diagonal dimension to the Vickers Pyramid Number, which varies inversely with the square of the diagonal. For this reason the standard deviation and values are correct only at the average Vpn. level and multiples cannot be applied directly to calculate the ranges. Range calculations were made before converting diagonal measurements to Vpn. values.

With these constituents and their various hardnesses occurring in different patterns it is doubtful if a conventional Rockwell or Brinell test has value unless it does not encounter or closely approach a primary carbide granule. If this stipulation is satisfied, it may provide some indication of structure as is the case with martensitic irons, but even thus it does not provide a reliable prediction of wear resistance. Table 9 presents several Rockwell hardnesses taken on matrix structures, with an attempt to avoid the primary tungsten carbides. The matrix hardness logically increases with the increasing opportunity to dissolve fine primary carbides, but the effect on high stress abrasion resistance is negligible for gas weld deposits.

If the abrasive is loose but has low contact stresses, a different pattern is expected. This has been studied by Haworth,[13] who used a rubber wheel. Its elasticity prevented crushing of the abrasive particles and also of brittle carbide particles. Under these conditions a correlation between carbide volume and wear rate was reported. As carbon increased the rate of abrasion fell.

Haworth established this relation on cast irons containing from 1% to 32% chromium, which probably are comparable with the range of matrix structures ob-

served in tungsten carbide welds. He also confirmed it on sintered carbide compacts, showing a loss of abrasion resistance against both quartz and alumina as the percentage of the cobalt binder increased. (Cobalt is not used as a binder in composite tube rods as described here, but it may be incorporated in cast carbide inserts that are used for hard facing.)

Table 9—Rockwell Hardness of Composite Gas Weld Matrices

Specimen	*Average Rockwell hardness*	*Initial wet-sand abrasion factor*	*Rod type*
R2413	52.1	0.17	100% 20/30 mesh granules
R2419	52.0	0.18	100% 30/40 mesh granules
R2397	58.0	0.18	100% 40/120 mesh granules
R2417	62.5	0.16	50% 20/30; 50%—120
R2425	62.3	0.16	50% 30/40; 50%—120

The rubber wheel test data also contain evidence that carbide hardness favorably increases wear resistance. Using the criteria of carbide hardness and carbide volume, inferences about the relative low stress abrasion resistance of the structures pictured in this paper can be drawn. Here again it may be concluded that gas welds, with less solution of primary granules and a greater volume of secondary carbides are likely to be superior to arc welds except where toughness complicates the matter.

HOT HARDNESS

The hardness at 1000 and 1200° F. for arc and gas welds made from tube rods containing 40/120 mesh carbide has been published.[1] To these have been added data at 600 and 800° F. on two of the specimens as summarized in Fig. 17. Each of the specimens was stabilized at the test temperature for 24 hr. before making hardness determinations. This minimizes or eliminates the effect of tempering on the apparent hardness. The effect of creep[1] is indicated by the numerals on the data points, signifying the time interval after load application.

The fine granule type of rod was chosen for these tests because complications from the large, very hard primary carbides are thus minimized. The intent of this set of tests is to reflect the matrix rather than some average between the granules and the matrix, which would be quite difficult to interpret since it would suffer seriously from sampling errors.

The difference in welding method is negligible up to about 1000° F., though the arc weld appears slightly softer. At 1200° F. a probable significant spread develops between the two. This is understandable as the greater granule solution during arc welding makes available more tungsten, with its hot hardness characteristics, for the matrix. The arc-weld superiority is evidenced by hardness level (Rc 38 average vs. Rc 28) for instantaneous values and by creep resistance (a drop of 26 Rc points vs. 44 points during 4 min. under load).

Since the gas vs. arc trends are opposite for hot hardness and wet sand abrasion resistance, analysis of service conditions should carefully consider both factors as well as the probable amount of undissolved primary carbides to be expected.

Tungsten carbide is not recommended for use much above 1200° F. because of its vulnerability to oxidation. At 1400° F. the usual granules will be completely oxidized in less than 24 hr.

PRACTICAL IMPLICATIONS

If this paper were intended solely for metallurgists, it would contain a summary section filled with technical conclusions. However, the purpose of the paper is more practical. While parts of it necessarily are involved with the details of metallography, statistics and testing in order to present the basic evidence, it is

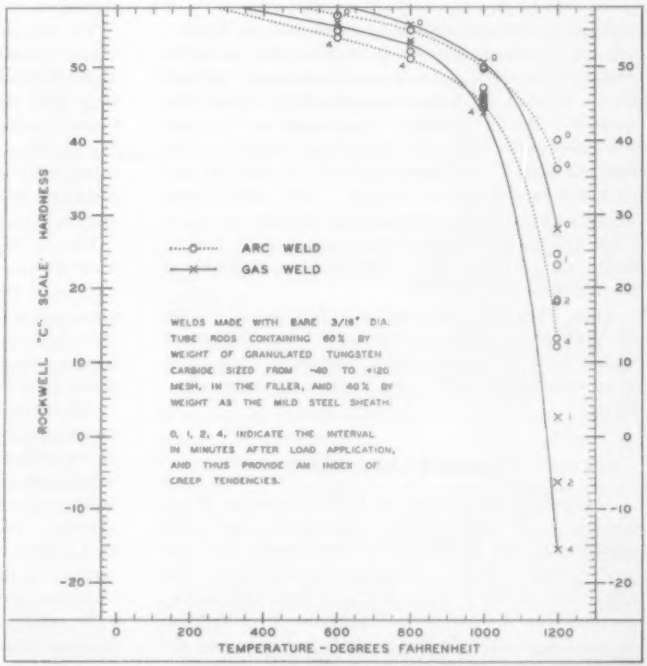

ROCKWELL "C" SCALE HARDNESS
TEMPERATURE - DEGREES FAHRENHEIT
ARC WELD
GAS WELD
WELDS MADE WITH BARE 3/16" DIA. TUBE RODS CONTAINING 60% BY WEIGHT OF GRANULATED TUNGSTEN CARBIDE SIZED FROM -40 TO +120 MESH, IN THE FILLER, AND 40% BY WEIGHT AS THE MILD STEEL SHEATH.
0, 1, 2, 4, INDICATE THE INTERVAL IN MINUTES AFTER LOAD APPLICATION, AND THUS PROVIDE AN INDEX OF CREEP TENDENCIES.

Fig. 17 The effect of temperature on apparent hot hardness of composite tungsten carbide weld deposits

hoped that the end results are more simple and useful in the field.

It has become obvious that tungsten-carbide tube rod welds are not simple structures with hard carbides in a soft steel matrix. Instead, the matrix may be very hard and is associated with a secondary hard carbide that forms during welding. The metallurgical details are complicated but lead to the following implications:

1. The best composite tungsten carbide overlays are likely to provide maximum abrasion resistance in comparison with other hard-facing materials (Figs. 4 and 8).

2. Requirements of surface smoothness should guide the selection of carbide granule size.

3. Gas welds should provide better resistance to high-stress abrasion than arc welds.

4. Because of carbon pickup the matrix of gas welds will probably be more brittle than that of arc welds.

5. The inclusion of fines in the granule quota of tube rods is likely to accentuate trends three and four above.

6. The inclusion of fines in electrodes may be detrimental to high stress abrasion resistance but should at the same time improve matrix hot hardness.

7. Those factors susceptible to control by the welder, such as arc power, penetration, dilution and carbide granule solution, may be used to diminish or accentuate the difference between arc and gas welds.

8. Very hot arc welding with fine granule tube rods may largely or completely dissolve the original tungsten carbides, with the loss of their distinctive high hardness (about 2400 Vpn.) and abrasion resistance.

While it may generally be assumed that the relative volume of primary carbides in the final weld deposit is probably an index of its abrasion resistance, caution should be used and inflexible applications of this idea avoided. Matrix character is important and in some cases may be decisive. It is possible to visualize service that can tear out the hard granules if they are not well anchored in a competent matrix. Also, undermining of the granules, with eventual loss, could occur with a loose, eroding abrasive operating against a soft matrix, which on the other hand might be very desirable if much impact were involved.

Thus, while this paper makes no pretense of solving all application problems, the use of its information should help the on-the-spot observer to draw more valid conclusions and to use hard facing more effectively.

ACKNOWLEDGMENT

The author is grateful to J. W. Gleassey, H. J. Chapin, L. W. Gordon, R. C. Carruthers, and other members of the Metallurgical Department for their contributions to the evidence presented here. The helpfulness of the Welding Rod and Metallurgical Departments of the American Manganese Steel Division in providing materials and welded specimens, and the permission of the American Brake Shoe Co. to publish this material are acknowledged with appreciation.

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Discussion by Gorham Woods

The paper is a valuable contribution to the meager metallurgical information that has been published on tungsten carbide weld deposits. We hope that there will be more of this work to keep pace with the rapid increase of the use of tungsten carbide surfacing.

We should also express our appreciation for Mr. Avery's work on abrasive wear which has given us abrasion factors for many of the wear resistant materials.

The apparent anomaly shown in Fig. 4 is of particular interest. Here we have an alloy (Cr-Co-W type) with a Brinell hardness of 450 which wore faster than mild steel. This is a good example of a case where hardness is not an index of wear resistance. We should add, however, that alloys of this type are superior for certain types of wear and also show high resistance to abrasion when tested on certain types of wear testing machines.

We are surprised at the very great difference in the wear resistance obtained with the arc deposits in comparison to the gas welds. This can be explained by examination of the wear samples. The weld sections show a great difference in the deposits, the arc welds having only a few of the tungsten carbide particles and also a poor distribution.

Arc deposition of the tungsten carbides is becoming more popular and we do not think that the arc deposits shown here are typical, for such deposits would not be satisfactory for field service. Arc deposits are now being made that compare favorably in service with gas deposits. As an example, in the resurfacing of worn tool joints, the arc application of tungsten carbide, applied by hand or automatically, gives the customer excellent service per dollar cost.

In order to obtain an arc deposit with a maximum number of carbide particles and with good distribution

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Fig. 1. 10 × Magnification cross sections through arc-weld deposits

Electrode composition: 60% 30 to 40 mesh, tungsten carbide, granules and 40% mild steel tube

Fig. 2. 10 × Magnification cross sections through arc-weld deposits

Electrode composition: 60% 30 to 40 mesh, tungsten carbide, granules and 40% mild steel tube

of the particles, the tungsten carbide used must have certain properties. Also, certain welding procedures must be followed.

Tungsten carbide particles that meet the following requirements are resistant to decomposition when arc deposited:

1. The particles should be compact chunks. Flake and sliver-shaped particles should be avoided.
2. The particles should be solid. Porous or spongy particles do not resist disintegration.
3. The particles should contain only tungsten and carbon. Some alloying metals will lower the melting point. Particles that are attracted by a strong magnet are contaminated with iron, cobalt or nickel, making them unsatisfactory for arc deposition.
4. The particles should have the optimum carbon content (about 4%) and a grain structure which gives maximum abrasion resistance.

Some tubular electrodes are furnished with a refractory coating on the carbide particles to give protection from the arc.

The following arc-welding procedure is recommended for deposition of tubular electrodes containing tungsten carbide particles:

1. Use straight polarity (electrode negative).
2. Use minimum welding current and hold a short arc.
3. Do not puddle the deposit. Beads are preferred to a wide weave.
4. Preheating is advisable for it enables the use of a lower welding current.

We think that arc deposits of tungsten carbide made as outlined above will compare more favorably with gas deposits than the arc deposits tested in securing data for the paper under discussion.

Discussion by C. E. Rogers

Mr. Avery's paper is a well presented summary of the conditions surrounding commercial use of tungsten carbide composite welding rods presented ostensibly as an aid to the user of hard-facing materials in evaluating field performance.

The general tone of this paper creates the impression that Mr. Avery is attempting to prove the superiority of gas applications of tungsten carbide composites over electric deposits of these materials. The results of hardness and wet quartz sand abrasion tests are presented as proof to substantiate this contention.

It should be emphasized that the wet quartz sand abrasion test or any wear test conducted under laboratory or semicontrolled conditions can provide information of a specific nature only and that information accumulated from such tests is of such a character that its use as a guide to the selection of materials by anyone other than those completely familiar with all the variable factors involved in making the tests is extremely hazardous.

For example, practical applications are more likely than not to include such important factors as impact, dynamic intermittent loading and flexing stresses which contribute to the disintegration of hard-facing deposits at least as much, and often to a much greater extent, than straight abrasive action.

The writer cannot condone the use of standard hardness tests in the evaluation of composite tungsten carbide deposits for reasons of unreliability which Mr. Avery, the author, himself points out. The wide use of scatter bands in evaluating hardness surveys indicates sufficient uncertainty to preclude their use by anyone other than the researcher and then only with caution.

The fact that gas applications of tungsten carbide composites generally serve to retain more of the original undissolved carbide material than is usually possible

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with electric applications does not necessarily detract from the importance of the electric method as a tool for applying hard-surfacing composites. Manufacturers accede to the demands of customers and furnish the best possible materials for the purpose required with variations in manufacturing procedure, where possible, to compensate for differences in usage.

The manufacturer supplys information and advice as to the choice of materials for the type of application the customer intends making, whether it be electric or gas. Indeed, the manufacturer has little choice in the matter, since some applications are suited only to the electric method, others only to gas or perhaps only one type of equipment is available for use.

Most manufacturers control the screen sizes of their tungsten carbide particles very closely, using 10–20, 20–30, 30–40 or similar ranges in their product. Few include a high percentage of fine materials with these mesh ranges, although most of the manufacturers have at least one rod made up of fine material. In making recommendations the seller must take into account the method of application as well as the mesh size of the material to be used for a particular application. This mesh size may only rarely be the same for electric application as would be recommended for a similar gas application. Since in practice it is necessary to select the proper mesh size for the particular application involved regardless of what method is used to deposit the material, the writer fails to understand how it would be possible to make such a selection from the results of the wear tests described by Mr. Avery.

Examination of the photographs of cross-sectioned arc and gas deposits leads to the natural inquiry as to whether abrasion during the tests did not take place on the matrix of the arc welds while during a comparable length of time the carbide particles in the gas deposits bore the brunt of the action. In actual service it is conceivable that the service life of the electric deposits would be such that relatively rapid initial wear would occur but that resistance to abrasion once the carbides were exposed would be somewhat comparable to the resistance of the gas welds.

Operators familiar with the application of composites by means of the electric arc are able to exercise sufficient control with automatic as well as manual equipment to secure a satisfactory dispersion of solid tungsten carbide particles vertically as well as laterally to provide good wear resistance for most applications to which the arc method is adapted. Actually there is a definite possibility of obtaining too dense a concentration of carbides in a relatively shallow electric deposit so that tungsten carbide granules are close packed in actual contact with each other and breaking out will occur with even moderate impact.

Refractory coated carbide granules will not dissolve as rapidly as granules which are not coated. The use of coated granules in electric rods is standard practice with one company and there is ample evidence to support the belief that such coatings are effective under average conditions in delaying the absorption of tungsten carbide into the matrix.

Mr. Avery treats lightly the inclusion of one per cent or so of magnetic material from crushing operations. We have examined rods sold on the market with as high as 2.5% of such material from crusher wear which, when considered in the light of the amount of tungsten carbide displaced in the filling of the tubes, becomes quite costly to the customer. Particularly so when one considers that the density of tungsten carbide is nearly twice that of steel.

One very important consideration overlooked by Mr. Avery is the shape of the tungsten carbide granule. Long, splintery or thin shapes will dissolve much more readily than grains which are somewhat cubic in shape with approximately equiaxial dimensions. The shape of the grain is determined by the type of crushing operation and the quality of the carbide.

The writer contends that regardless of whether the application is made by the arc or the gas method, providing it is done according to well-established practice by reasonably good operators, composite tungsten carbide hard facing is the best and by far the most wear-resistant material yet devised for applications where it is suited. The writer further contends, on the basis of a great number of wear tests devised and used over a period of 16 years in an effort to gather reliable information for general use, that the only reliable source of information for general usage is a practical field test. The practical test is the only test which will provide all the variables and factors involved for a particular application.

Crack Sensitivity of Aircraft Steels

Discussion by J. Koziarski

WE NEED MORE FUNDAMENTALS

(*A*) *Introduction*

Messrs. Steinberger, DeSimone and Stoop made a highly valuable contribution to the knowledge of welding with their work. Their paper makes an important link in a chain of publications on crack sensitivity of steels, started, I believe, by Messrs. Hoyt, Sims and Banta[1] in 1945. The latter work did not have enough echo, as, I think, it should have. This series of papers shows a modern approach to this important subject of welding technique and science and to this permanent headache of all welders and research workers.

The crack problem has been especially acute since 1928 or since the introduction into aircraft constriction of the low-alloy, high - strength chromium - molybdenum steel. During the war there was quite an extensive research program on crack sensitivity established in Germany. The summary of this work was published by K. L. Zeyen in 1943.[2] Before 1945 there was the tendency to blame high steel hardenability, high carbon content, unsuitable welding technique, etc., for welding cracks. Today we do not know what to blame. We know that neither high carbon nor high alloy content are the primary cause of crack. We may briefly summarize this as follows: "Before we did not know what we did not know, today we know what we do not know."

It seems that after those many years of research on steel welding crack sensitivity the following conclusions may be drawn:

1. Dissolved during heating and precipitated during cooling, hydrogen is the chief and immediate, although probably not the only cause of welding cracks.

2. Welding crack sensitivity depends among other things on carbide size and form and carbide stabilizing, carbide forming and carbide nucleating agents content in the alloy.

3. Higher carbon content or higher hardenability characteristics tend to increase welding crack sensitivity.

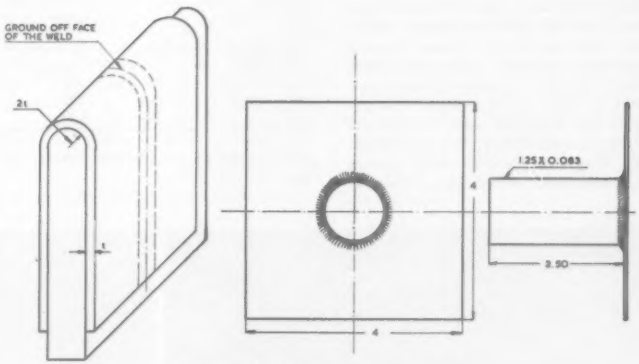

Fig. 1 Weldability test of thin sheets

(*a*) Bend test. (*b*) Tube welded to sheet (restrained welding test)

4. A suitable welding technique and especially a suitable pre- or postheating and a suitable source of heat may greatly reduce cracking.

5. Often in research we take the results of previous processes to which alloy was subjected as basic causes of our experimental results.

(*B*) *An Interesting Example*

I take the liberty to present a case from our Company's experience which, it seems, does not follow other authors' findings.

Sometime ago we were faced with a crack epidemic. This seriously affected our production. We had to devise a fast method which would allow us to counteract this difficulty. First of all we decided to classify our stock sheet material according to its weldability properties. For this purpose we used a simple and fast method of checking crack sensitivity. It consisted of a bend test (Fig. 1(*a*)) required by the Bureau of Aeronautics Spec. PW-7b, and a restrained welding test (Fig. 1(*b*)). The second test, consisting of a tube welded to sheet, was chosen because it represented very hard welding condition, it did not require any special fixture and was especially suitable to thin sheet gages. The bend test was made after the reinforcement of the weld was ground flat. The tube of the specimen of the second test was cut off after welding and the sheet was grid blasted and subjected to magnetic particle inspection. There was no pre- or postheating. Specimens were slowly cooled in asbestos dust. If no cracks appeared during these tests, the steel was considered as suitable for welding.*

I intend to discuss below 4130, chromium-molybdenum steel in sheet form, 0.049 in thick. Table 1 gives the chemical composition of the steel as required by Army-Navy material specification AN-QQ-S-685 and chemical analysis of steels as found on materials tested by us. Filler metal used was of a mild steel type, of $^{3}/_{64}$ in. diameter, conforming to Navy specification 46R4d Class 1, Type B.

Figure 2 shows photomicrographs of both sides of tube to sheet-welded specimens after the tube was cut off, the specimens was ground on both sides, grid blasted and magnafluxed. As may be seen, Steel A shows almost a continuous underbead crack. The same was true with Steel B. On the other hand, Steel C does not show any crack at all.

By comparing the results of Tables 2 and 3, one can see that Steels A and B satisfied all requirements of material

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Paper by A. N. Steinberger, B. J. DeSimone and J. Stoop was published in THE WELDING JOURNAL, September 1950, pp. 752-764.

* It must be stated that in the case of thin gages being used for the manufacture of parts with complicated geometry, it is not always possible or practicable to apply a suitable pre- or postheating. Consequently, such parts must be welded almost cold.

Table 1—Chemical Analysis, %

Designation	*Material*	*C*	*Mn*	$P_{max.}$	$S_{max.}$	*Si*	*Cr*	*Mo*
Specified	Sheet	0.27 to 0.33	0.40 to 0.60	0.040	0.050	...	0.80 to 1.10	0.15 to 0.25
A	Sheet	0.315	0.57	0.027	0.036	0.205	0.93	0.18
B	Sheet	0.317	0.51	0.023	0.026	0.316	0.91	0.18
C	Sheet	0.305	0.51	0.011	0.020	0.223	0.93	0.17
Specified	Filler metal	0.10 to 0.20	1.00 to 1.20	0.04	0.04	0.20 to 0.30	...	...
D	Filler metal	0.178	1.21	0.017	0.031	0.343	0.06	0.01

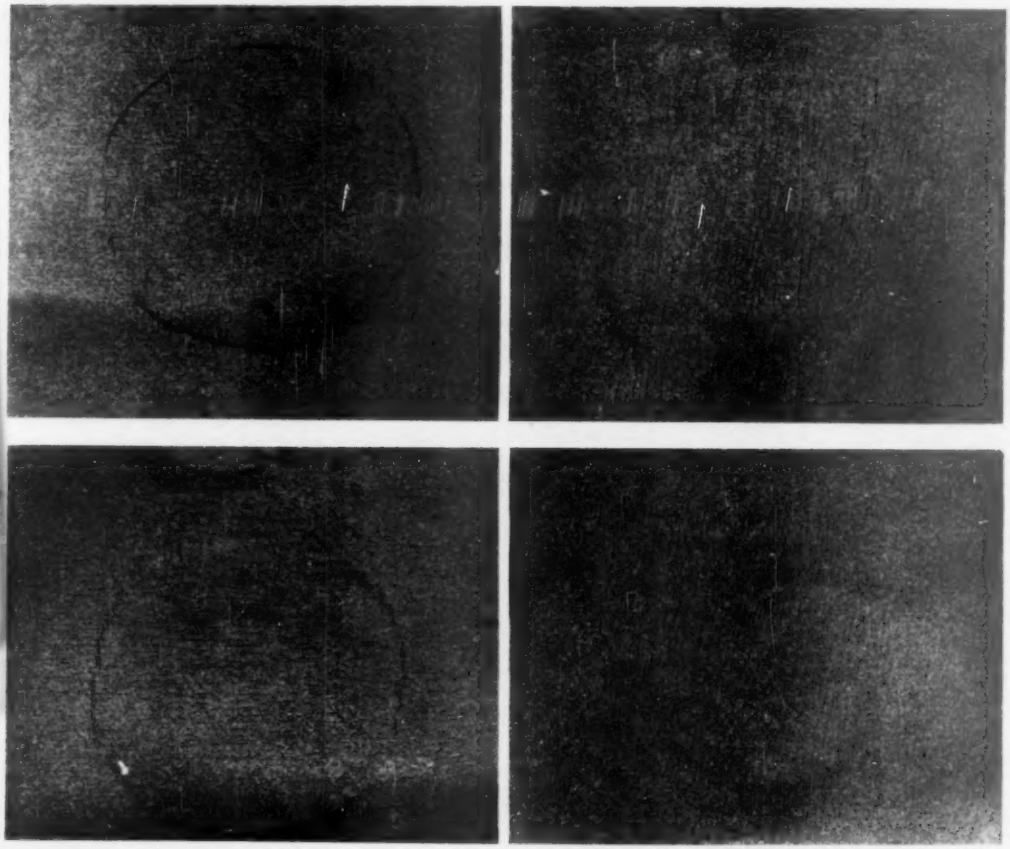
Table 2 gives physical properties of the sheet material as required by AN-QQ-S-685, as-received, in normalized condition.

specification AN-QQ-S-685, against which the material was bought, but showed inadequate weldability characteristics, while Steel C did not satisfy material specification. Although it had better physical properties, especially elongation, than Steels A and B, it cracked on bend test, but had excellent weldability. This unsatisfactory bend test result could be attributed to cold working, e.g., straightening. This was found correct, because after stress relief heat-treatment, Steel C satisfactorily passed all requirements of Spec. AN-QQ-S-685.

Table 2—Physical Properties

Designation	*Hardness Rockwell "B"*	*U.T.S., psi. × 1000*	*Y. P., psi. × 1000*	*Elong. in 2 in., %*	*Bend test*	
					Parallel	*Transverse*
Specified	...	95 min.	75 min.	10	No crack	No crack
A	95–99	115	93	10.1	No crack	No crack
B	98–100	118	101	10.8	No crack	No crack
C	98–99	120	101	17.1	Cracks	Cracks

Many attempts have been made to improve the weldability of Steels A and B. Renormalizing and annealing did not show any improvement. Some improvement



Steel A

Steel C

Fig. 2 Underbead crack. (Above) face side; (below) root side

Table 3—Weldability Characteristics

Designation	Bend test		Hardness	Tube welded to sheet
	Parallel	Transverse	Rockwell "C"*	
Specified by PW-7b	No crack	No crack	35	.....
A	Cracks	Cracks	33.5	Cracks underbead and in the weld metal
B	Cracks	Cracks	..	Cracks underbead and in the weld metal
C	No crack	No crack	..	No cracks or very few and very small superficial cracks

* Maximum hardness in the heat-affected zone.

could be seen after hydrogen relief, spheroidizing or spheroidizing and renormalizing. All, however, were unsatisfactory.

Table 3 shows weldability characteristics as required by specification PW-7b and as found.

Figures 3, 4 and 5 represent photomicrograph at 200 magnification of Steels A, B and C, respectively.

Steel A shows tempered martensite and Steel B bainite. Both contain small light grains which probably are carbides. This, according to our present knowledge, would explain crack sensitivity of these steels.

At first sight crack unsensitiveness of Steel C seems also to be clear. Many dispersed light grains might be taken for carbides, especially since, under the microscope, they are not as bright and shiny as ferrite. Magnification 500 (Fig. 6), however, brought some doubts whether these light grains were really carbides. Some of them were darker than the others. Microhardness test of these light grains gave values of between 135 and 223 V.H.N. They were much too soft for carbides but also too hard for ferrite.† Dark areas had Vickers hardness number between 143 and 333. Consequently, light grains could not be carbides and dark areas were probably sorbite. Unfortunately, because of the very fine grain structure, it was impossible to make any exact hardness impressions and measurements. Photomicrograph in Fig. 7 (x 1500) provided answer to the previous doubts. Light grains were ferrite containing very small globules of carbides. Probably these spheroidal carbides were responsible for lack of brightness and high hardness of the ferrite.

According to our pre ent knowledge Steel C should be very crack sensitive due to fine carbide grains. Actually, it was not at all sensitive. Due to lack of time and facilities we were not able to carry out further experiments with this steel.

† Boynton[2] gives the following Brinell hardness values for various metallurgical constituents (in the parentheses there are corresponding V.H.N.'s): ferrite—80 to 100 (80 to 100); cementite—700 (755); pearlite—200 to 250 (204 to 255); sorbite—250 to 400 (255 to 414); troostite—400 to 500 (414 to 540); martensite—500 to 650 (540 to 690); austenite—300 (308).

(C) *Is Crack Sensitivity Hereditary?*

There are still many questions to be answered and many problems to be solved in welding crack sensitivity. Some authors[3, 4] think that crack sensitivity is a hereditary characteristic depending on the steel making.

Welding cracks, however, are not only limited to steels. Cracks also occur in welding aluminum-silicon,[5] aluminum-magnesium[6] and other alloys.

We welders, however, are not alone to

Fig. 3

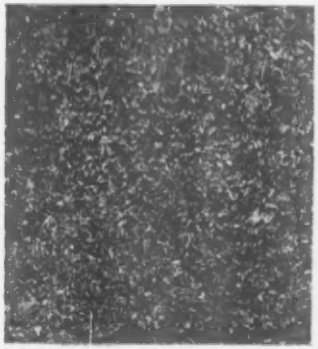


Fig. 4

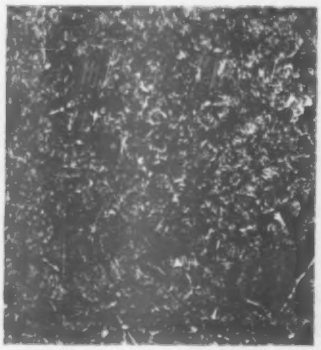


Fig. 5

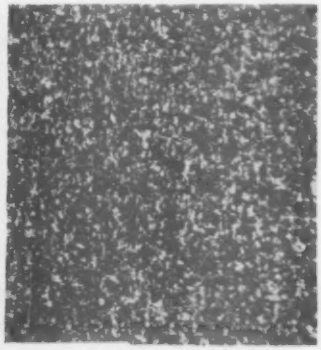


Fig. 6

Fig. 7

Fig. 3 Microstructure of Steel A. 2% Nital. × 200

Fig. 4 Microstructure of Steel B. 2% Nital. × 200

Fig. 5 Microstructure of Steel C. 2% Nital. × 200

Fig. 6 Microstructure of Steel C. (Note different shade of light grains.) 2% Nital. × 500

Fig. 7 Microstructure of Steel C. (Note ferrite with spheroidized carbides.) 2% Nital. × 1500

suffer from crack sensitivity. Similar headaches have been signaled by many scientific workers not necessarily connected with welding. An unexplained phenomenon in heat treatment has been recently reported by McDowell, Jr.,[7] and Spretnak and Wells.[8] They refer to "epidemics of cracks" as a characteristic of the steel heat. Graves[9] writes about "big mystery" "in variation in hot working properties of different heats of steels of the same chemical analysis."

As may be seen, others have similar troubles. A general tendency at present is to refer crack sensitivity of material to its birth; to the smelting furnace. Welding, besides crack sensitivity phenomenon, suffers from many other not always explained difficulties, such as inclusions, porosity,[6] etc.

(D) Conclusions

As may be seen welder and metallurgist have many common problems to solve and many common illnesses to cure. It is not surprising because both deal with metal. This is the reason why a welder looks for help from a metallurgist. Unfortunately, he cannot always get this help because the metallurgist himself is often helpless and because he often thinks in different categories than the welder should think. Although welding may, up to some extent, be compared to die casting it would be wrong to try to apply strictly die casting rules to welding. Also conventional metallurgy, although the basis of welding, cannot provide satisfactory answers to all welding questions. An average metallurgist thinks in terms of pounds or tons, when quality of metal is involved, in minutes or even hours and in relatively low and uniform temperatures, when considering metallurgical processes. A welder, on the contrary, should think in terms of grams or even of fractions of a gram, in seconds or in fractions of a second and steep temperature gradient, with sometimes hundreds of degrees difference on a small distance. Besides, in welding, temperatures may range from room temperature or below, to—no one knows.

As is known, every physical-chemical reaction (I purposely avoid the term chemical reaction) depends on three factors: time, temperature and pressure. We can relatively easily measure the time, but often temperatures and pressures remain unknown. For instance, up to recent years, the temperature of the electric arc, in electric arc welding, was believed not to exceed 5500° C. Recently, however, Prof. Cobine[10] reported that the temperature of the center of high current arc may be of the order of 11,000° K. (10,727° C.). And what pressures exist inside of such an arc? We do not know. But, if we do not know the temperature and the pressure in an electric arc, how can we know what happens to the metal or electrode coating material particles when the electrode core is melted and metal particles transferred to the weld? Do we also know enough about nucleation or modifiers? We try to fight cracks, porosity, inclusions and we do not have suitable tools to do the job. An old principle of military strategy requires: know the enemy first and next try to fight him. We cannot successfully combat our enemies because we do not know enough about them. I come back to my initial statement: "Today we know what we do not know." We do not know enough about fundamentals. It seems that before these fundamentals are mastered there is no hope of real progress in welding.

In solving these fundamental problems more help may be expected from physicists and physical-chemists than from metallurgist. Or more correctly: a true solution of welding problems may be assured by teamwork of scientific and technical workers, such as physicists, physical-chemists, chemists, metallurgists and welders. Such cooperation, however, does not seem to exist today. Two years ago, I had an opportunity to talk in Europe to some physical-chemical scientists. They were rather surprised to hear what fundamental problems we are interested in. They complained that they were not told by the metallurgists what they needed. In fact, they stated, there was no true collaboration between physical-chemistry and metallurgy.

One may say that fundamental research is expensive. Yes, it is. But how much money and energy is spent every year throughout the world on experimental research? Pure experimental method, in the end, may bring us to the solution, but it must satisfy the calculus of probability and, consequently, the experiments must be very numerous. This would be very expensive. Would it not be better to spend some money and energy on fundamentals and, after the fundamental facts are known, to have them confirmed by experimenting? In one particular case of electric arc welding, let the physicists find the temperatures and pressures in the electric arc, and then let the physical-chemist find the physical-chemical reactions and transformations metal particle undergoes from the time it is melted on the electrode core to the moment it is cooled. And finally, let the welding metallurgists (not metallurgists) and welders find solutions to our everyday difficulties. Let a welding research worker act in a full knowledge of fundamental facts instead of requiring him to work blindfolded. Let him first know the fundamental ABC before asking him to write a novel.

ACKNOWLEDGMENT

I wish to express my thanks to the management of the Piasecki Helicopter Corp. and to L. L. Douglas, Chief Engineer, for the permission to use the Company's data and making available Company's facilities in the preparation of this discussion. My thanks are also due to Dr. A. Grosvenor, Professor of Metallurgy, Drexel Institute of Technology, for his help in making micro hardness tests.

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"Heliarc" Welding in Production

Discussion by Robert Bowman

One question which often arises in connection with the inert arc welding of thin stainless steel aircraft parts is whether argon or helium should be used. This question was presented to the Air Materiel Command when a maintenance depot requested argon instead of helium for inert arc welding of thin stainless steel jet engine parts. It was stated that helium was not suitable for welding thin stainless steel parts because the arc was too "hot." In order to have more quantitative information on this question, an investigation was conducted to determine the energy generated at the arc in the two gases and also the ease with which a stable arc could be established. Some data were available on the voltage drop in the arc but these data were obtained under conditions somewhat different to those in welding thin stainless steel parts.

The arc drop in the two gases, helium and argon, was measured under conditions similar to those encountered in actual welding. The equipment used in these measurements consisted of:

(*a*) D.-c., 200-amp. aircraft arc welder.
(*b*) Standard torch used arc welding with the inert gases.
(*c*) D.-c. ammeter and d.-c. voltmeter.
(*d*) Lathe for moving sheet under torch.

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Paper by T. E. Piper was published in the September 1950 WELDING JOURNAL, pp. 723–728.

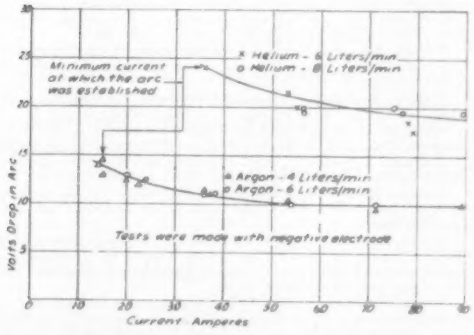

Fig. 1 Voltage drops through arcs in Argon and Helium

The torch was clamped in a fixed position over a $^1/_8$-in. stainless steel sheet which was mounted on the carriage of the lathe. The feed was adjusted so that the carriage traveled at the rate of $5^1/_4$ in. per minute. A $^1/_{16}$-in. diameter tungsten electrode was used and it was adjusted for an arc length of $^1/_8$ in. The end of the electrode was ground to a sharp point (45° included angle) to assist in starting the arc at the lower currents. After all adjustments and connections were made, the arc was started by shorting momentarily the tungsten electrode to the sheet with another tungsten electrode. The sheet was moved under the electrode when the arc was established. The voltage drop across the arc was measured at different currents and rates of gas flow. The results for the two gases, helium and argon, are plotted in Fig. 1.

It will be noted in Fig. 1 that the arc drop for a given current in argon is approximately one-half of that for helium. The minimum current at which the arc could be established was approximately 15 amp. for argon and 35 amp. for helium. When these facts are taken into consideration, it will be noted that the heat generated at an arc in argon can be adjusted to a minimum one-fourth of that for helium. Practical welding tests indicate that it is possible to use helium for hand welding stainless steel sheet above $^1/_{16}$ in. in thickness. In welding thinner sheets, the use of helium becomes very difficult. Welding of 0.032-in. stainless steel sheet was readily accomplished with argon but not with helium. When the current was adjusted high enough to estab-

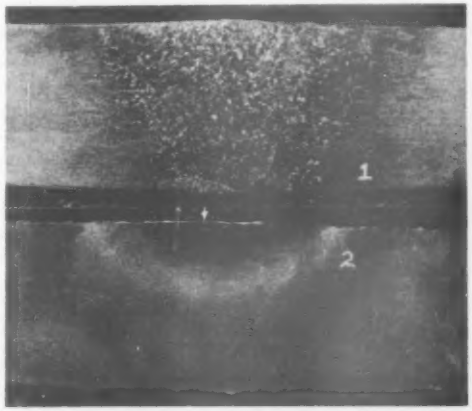

Fig. 2 Penetration produced by the arcs in Helium (1) and Argon (2)

lish an arc in the helium, the arc burned a hole through the 0.032-in. sheet.

Under similar conditions an arc in helium will produce more penetration in the sheet than an arc in argon. This is explained by the greater arc drop in helium. This is illustrated by Fig. 2 which shows sections across the path of the arc on $^1/_8$-in. stainless steel sheet. The two specimens were prepared in the manner described. Specimen No. 1 was prepared using helium with flowmeter indicating 6 liters per minute, the current at 53 amp. and 20 v. across the arc. Specimen No. 2 was prepared using argon with flowmeter indicating 6 liters per minute, the current at 52 amp. and 10 v. across the arc. The two gases can be compared by observing the small penetration at the arrow on Specimen No. 2 made with argon and the complete penetration in Specimen No. 1 made with helium. Complete penetration was obtained with argon by increasing the current to 100 amp. An arc of 100 amp. in argon will generate approximately the same energy as an arc of 50 amp. in helium.

The results obtained in this investigation, indicate that argon is more suitable than helium for hand welding thin gages of stainless steel sheet. The arc in argon can be established at lower currents and also the arc drop in argon is one-half of that in helium for the same current. The lower arc drop produces less penetration which is desirable in welding thin sheets. Helium, however, will be desirable in welding the thicker gages.

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Plan Not Little Things

Designers of machinery and other products should not resist new ideas or permit tradition and empirical relations to cramp their thinking. They should attack problems with a positive aggressiveness that bespeaks success

by G. F. Nordenholt

UNDER the glass desk top of T. A. Evans, a great engineer who designed the Wacker Drive here in Chicago, and many similar civil engineering monuments, appeared the motto, "Plan Not Little Things." His creations were all big things, physically—bridges, roadways and other civil engineering structures. Perhaps some of you mechanical engineers are thinking that only civil engineers have the opportunity to plan big things. But it is a mistake to think that "big things" in the sense that the term was used in the motto on Evan's desk must be large physically. Most of the world's greatest discoveries and creations have been little things in physical size. Louis Pasteur's great discovery related to the then invisible germs. Galileo developed the telescope to look at the universe. Fulton's steamboat was a clumsy little contrivance. Faraday's invention of the generation of electricity was nothing more than a little permanent magnet and a small coil of wire. Yet all of these creations, discoveries and inventions, big or little in physical size, were the work of men who planned not little things and brought forth wonders. They dedicated their lives to bring to fruition their great achievements which have contributed to the building of a material world undreamed of a century ago.

One might well ask what are the earmarks of men who "plan not little things?" How do such men operate mentally? What are some of the keystones of their philosophy? What is that that distinguishes the mental processes of a great creative scientist or engineer from ordinary thinking?

On the wall of the office of the vice-president in charge of engineering of the Brown & Sharpe Mfg. Co., Ben Graves, whom many of you may know, hangs a sign, "It is wonderful how the human mind can resist a new idea." That, ladies and gentlemen, is the answer to the last question. Ordinary minds resist new ideas and thereby their thinking is confined to the boundaries of existing knowledge. Creative minds welcome new ideas, study them, analyze them and even try them, all without prejudice or preconceived notions. The all too common procedure of the ordinary mortal is to do some quick, shallow thinking and come up with a horde of negatives to prove that the new proposal is unworkable, unsound, impossible, ridiculous, merely theoretical and unworthy of further serious consideration. That, in a nutshell, distinguishes the truly scientific approach from the improperly labeled practical approach. The scientist and truly progressive engineer studies, analyzes and weighs. He strives to overcome obstacles; he is positive in his thinking and detests the negative. He invents, discovers, and builds new fields of knowledge, and in the process he often proves the fallacies in existing concepts.

Of course, we engineers, you might unanimously agree, fall into the scientific category of thinking. We don't have to prove it—we admit it. But even the best of us are all too likely to go off the deep end into the abyss of fancies every once in awhile. Most of you know for a positive fact that tap water at sea level boils at 212° F. And if I were to offer to bet you that I can heat pure water to 222° F. without it boiling, you might take me up on it. Save your money. You can heat chemically pure water to 230° F. without it boiling. And in the freezing process, chemically pure water will remain liquid water at 20° F. Only impure water boils at 212° and freezes at 32 degrees. And if you put the water in a shallow pan on a bed of wet straw on a clear night on the plateau of Tibet, you can collect a nice bunch of ice cubes in the wee hours of the morning and mix yourself a potent drink—even though the temperature may never have dropped below 35° F. That in spite of the fact that 32° F. is the published melting temperature or freezing point of ice.

Yes, our minds are cluttered with diluted facts, fan-

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cies, and half-truths. This is especially true of design engineers. We learn in college that unit tensile stress is equal to the total pull divided by the cross-sectional area. But at least a dozen conditions will appreciably modify this relationship, which is usually only about 80% true anyhow. For a given weight per foot of length, a hollow shaft is stronger than a solid one, but under sudden reversals and high inertia the hollow shaft snaps while the solid one winds like a spring to absorb the shock. And it does not break, because it is resilient. And so it goes with dozens of so-called facts. They are true only under certain conditions. Yet often will a man make a closed decision on the basis of such conditional facts.

Any one of three mental attitudes may be exercised by the individual when he faces something new or embarks on a new venture. He may be negative, or zero or positive. Let's forget the negative boys. Rarely can a man achieve a responsible engineering position if he is just a perpetual negative. Hence I don't think there are many negative minds among us. But sad to state, too many engineers are just plain zero, and that is an awful state to be in. I have often thought that if it is really true that nature abhors a vacuum, why does she permit so many minds with zero attitudes. The third group, those with the positive attitude, are the ones to whom we must look for progress.

The bellyachers bemoan the impediments, the kickers blast the impediments aside. When the late Edward G. Budd found that he could not build stainless steel cars because they would have to be spot-welded, and stainless steel couldn't be spot-welded at that time, he made a daring decision. He would find out how to spot-weld stainless steel. And he did. And then he built and sold the finest fleet of stainless steel cars in existence.

When in 1940 Charles Guernsey, then Chief Engineer of J. G. Brill, was faced with the necessity of changing from aluminum to steel for his busses, he accepted the challenge of making a steel bus that weighed no more than an aluminum bus, in spite of the fact that steel weighs three times as much as aluminum. By brilliant designing and the use of welding, he created a steel bus that weighed 2000 pounds less than its aluminum predecessor.

Many more such examples can be cited. In each instance the creators did not permit their thinking to be clouded by prejudices or hemmed in by considering only one phase of the problem. Nor did they allow themselves to be defeated by traditions, empirical relationships or rational formulas that didn't really apply. They planned big things and to achieve their goal they thought big. They studied their problem in all its aspects.

Many years ago, about 25, whenever the question of welding was brought up, the discussion immediately degenerated into welding vs. riveting. Many of the meetings of the AMERICAN WELDING SOCIETY were given over to heated discussions of that question. As I remember it, the arguments were all pro welding and con for riveting. Anyone who had the temerity to reverse the argument might have been accorded the same treatment as that given to a union member who proposed a cut in hourly rates. I don't really know because no one ever argued contrariwise.

In the twenties many design engineers recognized welding only as a good tool for repair jobs. A few of them, notably among them Jim Owens, then Welding Engineer at Newport News Shipbuilding and later with Fairbanks Morse, had the guts and vision to put welding to use as a design and production tool in a big way. Owens had the courage to bet his future on the application of welding to shipbuilding and he won. He also won, and very properly so, the first of the grand prizes offered by Lincoln Electric. Jim Owens did not plan little things.

With reference to welding, or any other production methods for that matter, how should the design engineer view the problem in a big way? This afternoon you will hear some excellent papers on this subject. The papers will go into detail. I will merely mention some of the philosophical principles that might guide your thinking.

Designers are never faced with the problem of "How can I use welding?" Their problem is always, "How can I achieve this or that end result?" The question as to using welding comes into the picture naturally because every design involves the problem of joining parts. Even if the design is solely that of a casting or forging, there will be the question as to whether or not it would be better to make it in two pieces and weld it together. But let me get specific. Here are some of the things to consider.

1. Direct costs. These are often only incidental. The direct cost of welding might be more than that of riveting, but do not permit that to decide you. The final total cost is usually less.

2. Space saving. This in itself might be inconsequential, but on the other hand it usually makes possible big savings in the size of associated parts.

3. Reduction in number of parts. There are many intangible savings in the reduction of number of parts. These include handling costs, stores, purchasing, obsolescence and many other factors.

4. Combining parts made by different production methods, such as forgings and hot-rolled pieces, castings and rolled sections and other combinations.

5. Use of higher priced, more suitable materials at less total cost. Stainless steel and fine silver can be used economically in welded designs for certain purposes.

6. Improved appearance and simplified designs.

The above six considerations are mere generalizations. There are many more, and each covers a wide territory. You'll hear more about some of them in detail in the papers this afternoon. But assuredly, they must impress you with the fact that the engineer must think and reason in a big way when he contemplates the design of his product. It is almost inevitable that he will have to include the possibilities of welding

in his studies. If he ignores welding entirely, he is not planning big things.

Designers of machinery and other products will never go wrong if they do not permit their minds to resist new ideas, if they plan not little things, if they do not permit tradition and empirical relations to cramp their thinking and if they attack problems not with a negative attitude nor with a zero stagnation, but with a positive aggressiveness that bespeaks success. Those are the earmarks of the men who plan not little things.

Food Cabinet Production Jumps 2600% Through Use of Resistance Welding

THE Nashville Corp., Nashville, Tenn., has proved that cabinets and gas and electric ranges can be designed for fabrication by the resistance welding method to effect enormous production increases and the consequent lowering of costs.

In the frozen-food cabinet division of Nashco, where ordinary welding techniques were formerly employed, the welding time-per-part averaged out to 27 min. This fabricating method also required skilled welding technicians and a considerable investment in welding wire and flux rod.

After installation of the resistance welding equipment and redesigning of the cabinet units for greater adaptability to resistance-welding requirement, it was found that the welding time-per-part could be cut to a fraction under 1 min., thus effecting a production potential of 2600%.

Figure 1

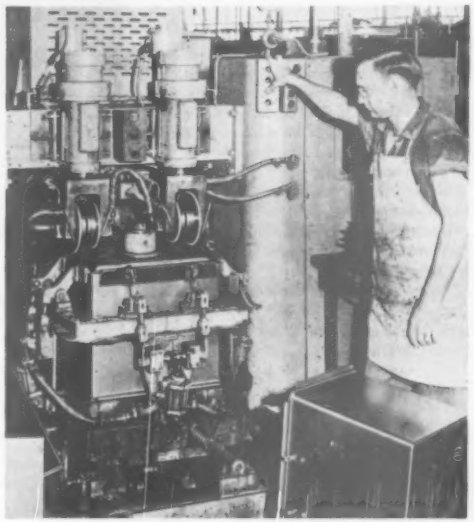


Figure 2

Applying this same design-for-resistance welding method, Nashco revamped the manufacturing processes on its gas and electric ranges. On the range front frame, for example, company engineers designed five stampings which could be placed in a jig-like frame and automatically positioned properly and, again automatically, resistance welded in a fraction of the time that former welding methods permitted. By the new method, the company produces 68 of the range frames each hour.

In the finishing of the range, the company proceeded to convert to resistance welding in wholesale fashion. Out of nine assembly operations required, all but two are effected by the resistance-welding process. The two involve the installation of the cable bracket (arc welded) and the japanning of the finished job (a heat-treat paint process).

The accompanying photographs illustrate resistance welding at Nashco.

Hard-Face Cultivator Sweeps for Longer Wear

by L. C. Richmann

WHERE there is high abrasion, wearing surfaces can be protected with a hard-facing deposit to prevent excessive wear. Cultivator sweeps are good examples of the type of surfaces exposed to continuous severe abrasion.

The normal usage of these sweeps in slightly sandy, pebbly soil is approximately 150 acres. It is then necessary to replace them with new sweeps which cost approximately $1.25 apiece. This becomes an expensive operation.

However, by hard-facing cultivator sweeps before they are put into use, they will last three times longer than unprotected sweeps. Here is how to do it.

PREPARING THE WORK

First, select a good hard-facing rod. Then grind or file the top of the sweeps for an area of approximately $^1/_8$-in. back from the edge of the blade. Clean the area well being sure to remove all rust, dirt and scale.

Attach a medium-size welding tip to your blowpipe and adjust it for an excess-acetylene flame. Then heat the part slowly. Hold the blowpipe so that the flame points at the metal at an angle of from 30 to 60°. Heat the area to be hard faced until the metal turns red and begins to sweat. Do not direct the flame at the thinnest edge of the blade as too much heat will cause it to warp.

L. C. Richmann, The Linde Air Products Co., Minneapolis, Minn.

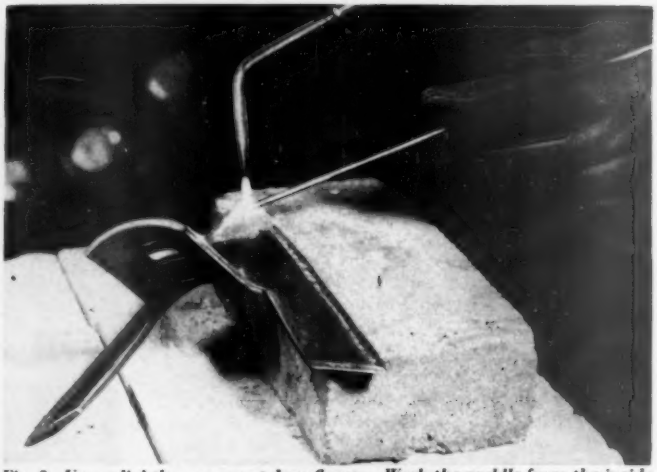


Fig. 2 Use a slightly excess acetylene flame. Work the puddle from the inside of the blade toward the cutting edge

DEPOSITING THE ROD

Heat only until the steel starts to sweat—do not melt it. Then deposit the hard-facing rod, working the puddle from the inside of the blade outward towards the cutting edge. Apply a thin smooth deposit about $^1/_8$-in. thick. Keep the rod reasonably still, moving it in small very tight circles at the front of the puddle.

Try to cover the entire surface that is to be hard faced in one operation. If it is necessary to stop the work, or to go back to reweld small cracks or holes, be careful to melt only the top surface of the deposit and then add the rod. Do not go back more than an inch or two from where welding was stopped. To prevent cracks or holes, draw the flame away slowly when you finish a deposit.

COMPLETING THE JOB

When you finish the job, let the sweep cool slowly in the air. Normally, further hardening will not be necessary. You will not need to sharpen the sweeps by grinding. They will resharpen themselves as they wear.

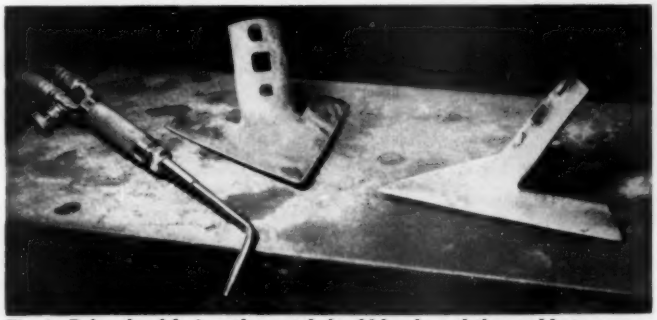


Fig. 1 Before hard facing, the metal should be cleaned thoroughly to remove all dirt, scale and rust

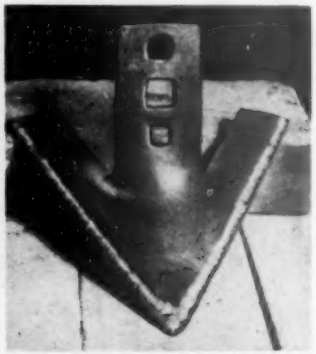


Fig. 3 This hard-faced cultivator sweep will wear three times longer than an untreated sweep

Hints on How to Weld Sheet Lead

by E. J. Chester

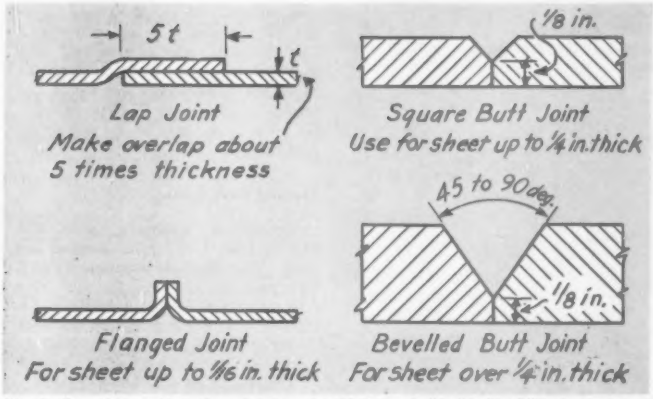


Fig. 1 Practice making these four types of joints used in lead welding operations

WITH some practice, you'll find that lead welding is not too difficult to do. The important thing to remember is that the heavy, soft, dull gray metal melts quickly even under a small blowpipe flame. Large pieces of lead will even collapse under their own weight during welding if they are not supported properly. You have to be careful therefore not to use too much heat. This article gives you hints on making some of the simpler joints used in lead welding.

Table I —Tip and Rod Sizes for Welding Lead

Sheet designation, lb.	*Sheet thickness, in.*	*Welding rod diameter, in.*	*Diameter of flame port drill size*
1	1/64	1/32	79
2	1/32	1/16	74
4	1/16	1/8	70
8	1/8	1/4	70
12	3/32	1/4	70
16	1/4	3/8	70
32	1/8	3/8	65
48	3/4	1/2	56
60	13/16	3/8	56

E. J. Chester, The Linde Air Products Co., Detroit, Mich.

GETTING READY TO WELD

The first step in getting ready to weld is to choose a welding tip and a welding rod. Table I gives rod sizes, and will help you choose the proper tip size. Rod should be of the same type metal as the parts you are welding. For large jobs, homemade rod can be made by cutting strips from sheet lead. Always scrape the rod clean just before welding.

Some simple lead joints you can try making are shown in Fig. 1. Lap and flanged joints are made by bending the sheets. Use a knife to prepare butt joints. First cut the vee with a knife, then scrape the facing edges clean. Tap the sheets with a mallet for a close fit.

MAKING A LAP JOINT

To make a lap joint in sheet 1/8 in. thick, first scrape the exposed edges and overlapping surface of both sheets. Then tap the joint with a wooden mallet to bring the sheets into close contact.

Use the welding tip recommended in Table I, and set the regulators for the pressures recommended by the blowpipe manufacturer for lead welding. Adjust the blowpipe for the tiny, pointed, neutral or slightly excess acetylene flame shown in Fig. 2.

After you get the proper flame, hold the blowpipe so that the flame is pointed

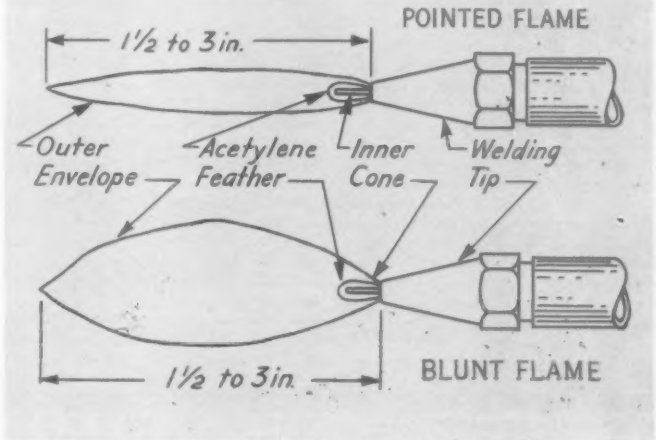


Fig. 2 Always consult Table I for a welding tip. Use the pressures recommended by the blowpipe manufacturer

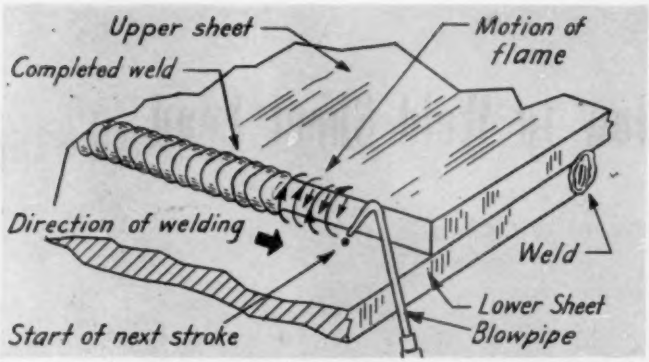


Fig. 3 Make lap welds by moving the flame in a series of short arcs toward the overlap, then straight away

Fig. 4 This partially completed lap weld in vertical sheet shows how lead from the top sheet is melted down to form the weld

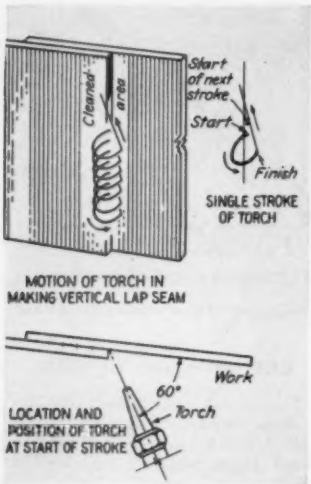


Fig. 5 Move the flame in a series of small, overlapping circles from the underneath sheet to lapped edge and back

straight at the surface of the work with the end of the inner cone almost touching the surface. Make the weld by moving the flame in a series of arcs toward the overlap and then straight away as shown in Fig. 3. Heat a spot on each sheet until a tiny puddle forms. Concentrate the flame a bit more on the lower sheet so that it will melt at the same time that the upper sheet melts. As soon as the puddle forms, fusing the two sheets together, lift the blowpipe away quickly. Melt a new spot, overlapping the first, the same way. The idea is to make a series of spot welds.

MAKING A BUTT JOINT

To make butt welds in lead sheet 1/2 in. thick, prepare the edges as shown in Fig. 1. First scrape both edges to be joined with a knife; then bevel an angle between the butted surfaces. Tap lightly with a wooden mallet to align the sheets. Next scrape the end of a 3/32-in. lead rod with the knife. Set the regulators as you did to make a lap joint. Again, adjust the blowpipe for the pointed slightly excess acetylene flame.

Hold the blowpipe flame at one end of the joint and place the end of the rod in the flame. A drop of metal from the rod should be added just as the edges of the nose of the butt joint melt together. Deposit a drop of rod at a time along the bottom of the vee. When you weld the other end of the joint, let the weld cool a little, then wire brush it.

Now adjust the blowpipe for the blunt flame shown in Fig. 2. Deposit another layer of weld metal right on top of the first layer. Melt the rod metal you deposited before and the beveled sides. Then add a drop of rod at a time until you reach the other end of the joint. Make as many more passes as needed until you fill the beveled area with rod metal.

VERTICAL WELDING

Vertical Lap Joint

Start welding at the bottom of vertical seams and work upward as shown in Fig. 4. If the lower end of the weld does not rest on another lead plate, use a small plate or rod to support the first drop of molten weld metal. When this drop hardens it will support the next overlapping drop.

First clean the areas of both over-lapping surfaces. Move the blowpipe flame in a circular motion as shown in Fig. 5. The overlapping edge supplies weld metal that gives a rounded finish to the joint. Move the flame higher and repeat this process as soon as the first puddle solidifies. Carry the weld to the top of the joint this way.

Vertical Butt Joint

Now practice making a vertical butt weld, in lead 1/2 in. thick, using an iron mold. Make the mold as shown in Fig. 6. First place the mold over the joint, then melt the beveled edges and welding rod so that they fuse together and fill the mold. Be sure that the lead in the bottom of the mold is in a molten state before you add more lead from the rod.

THINGS TO REMEMBER

Remember that it takes practice to make strong joints in lead. Practice on scrap lead pieces. Try flame adjustments, and practice moving the blowpipe and the welding rod.

Proper ventilation is important so that you do not inhale any harmful fumes during welding. Work outdoors or in a well ventilated place. If you must work in confined areas, use a blower to blow away the fumes.

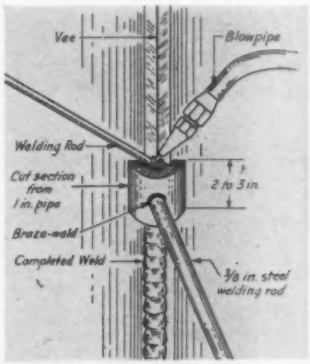


Fig. 6 An iron mold will help you make vertical butt welds

Adjusting the Oxyacetylene Flame

by R. G. Harner

BY A simple twist of a valve, you can adjust the oxyacetylene flame so that it can be used for a wide variety of jobs in the metal-working field. It can be used for such jobs as fusion welding, braze welding, hard facing, cutting, as well as heating for bending, straightening and forming metals.

The three basic types of flames which make these jobs possible are the neutral flame; the excess acetylene flame, often called a carburizing or reducing flame; and the excess oxygen or oxidizing flame. The wide range of uses to which these three flames can be put make the oxyacetylene blowpipe one of the most valuable and versatile tools in the metal-working industry.

Here is how to adjust for each flame and what you can do with each type.

Fig. 1 Neutral flame. This flame has an approximate 1 to 1 ratio of oxygen and acetylene. It consists of two parts only—the inner cone, and the outer envelope

NEUTRAL FLAME

What It Is

The flame is neutral when an approximate 1-to-1 mixture of oxygen and acetylene is lighted at the blowpipe tip. The term "neutral" means that the flame contains neither an excess of oxygen nor an excess of acetylene and that the effect of the flame on molten metal during welding or heating operations is chemically neutral; that is, it does not burn, oxidize or add carbon to the metal.

How to Adjust For It

After adjusting the regulator valves to proper pressures (depending on nozzle requirements) open the oxygen valve on the blowpipe, 1/4 turn, and open the acetylene valve 1 full turn. Light the blowpipe with a friction lighter. Next, open both valves wide. There will be an excess of acetylene, and the flame will have three distinct parts.

1. There will be the inner cone, a thin tongue of flame which burns with a brilliant white light.

2. Surrounding this will be an intermediate cone of whitish color known as the acetylene "feather." The length of this intermediate or excess acetylene cone varies directly with the amount of excess acetylene in the flame.

3. Surrounding both of these cones is a bluish outer envelope. By closing the acetylene valve very slowly, the whitish intermediate cone or feather can be made to grow smaller and smaller until it finally disappears completely. When it just disappears you have a neutral flame, as shown in Fig. 1.

Where It Is Used

This flame is important for two reasons. First, it is used for fusion welding and heating operations. Second, it serves as a reference flame from which other flames are adjusted. The neutral flame is used for fusion welding such common metals as cast iron and steel; steel sheet, plate and pipe; wrought iron; chrome and chrome-nickel steels; and copper and lead. It is also used for heat treating, and heating metals for bending, forming and straightening.

EXCESS ACETYLENE FLAME

What It Is

The excess acetylene, or carburizing flame, is marked by a prominent intermediate cone or acetylene feather, as shown in Fig. 2. This feather is caused by tiny unburned particles of incandescent carbon. As these particles reach the outer edge of the flame, they are burned and disappear.

How To Adjust For It

First, light the blowpipe and adjust the flame to neutral. Follow the directions given above. Now increase the amount of acetylene until a feather of the proper size is obtained. Some jobs require 2x and 3x acetylene flames. This means that the acetylene feather is two or three times the length of the inner cone, as shown in Fig. 2.

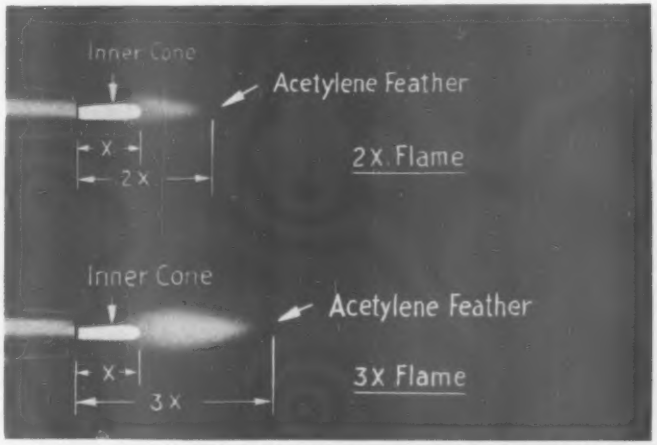


Fig. 2 Excess acetylene flame. The excess amount of acetylene in this flame causes the intermediate cone or acetylene "feather." By varying the length of the "feather" excess acetylene flames for different purposes may be obtained

R. G. Harner, The Linde Air Products Co., Cleveland, Ohio.

Where It Is Used

The excess acetylene flame is important because it is used for the majority of hard-facing jobs. Varying degrees of this flame are likewise used for fusion-welding such metals as high carbon steel, aluminum, nickel, Monel and Inconel.

EXCESS OXYGEN FLAME

What It Is

The excess oxygen flame can be recognized by its general resemblance to the neutral flame. (See Fig. 3.) However, the inner cone will be transparent and of a dark blue tinge. The cone of the excess oxygen flame will also be somewhat shorter, more sharply pointed but less clearly defined than either the excess acetylene or neutral flames.

How To Adjust For It

The excess oxygen, or oxidizing flame, is obtained by first adjusting to a neutral flame. Then the amount of acetylene is decreased to get an excess oxygen flame.

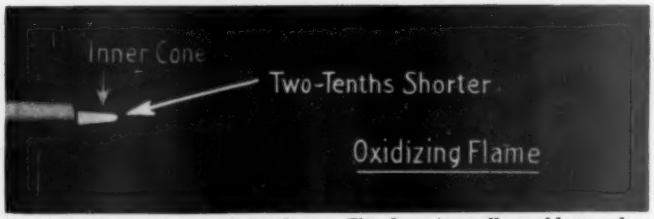


Fig. 3 Excess oxygen or oxidizing flame. This flame is smaller and hotter than either the neutral or excess acetylene flame. The excess amount of oxygen causes the inner cone to shorten or "neck in"

The more excess oxygen contained in the flame, the shorter the inner cone of the flame will be.

Where To Use It

The excess oxygen flame is important because it is used for all bronze-welding jobs. It is used also for fusion welding manganese steel and Everdur metal. Care must be taken when using the excess oxygen flame not to oxidize or "burn" the metal. In the case of brass and bronze, the exact adjustment should be determined before welding by testing the action of the flame on the metal first.

"SLIGHTLY EXCESS" FLAMES

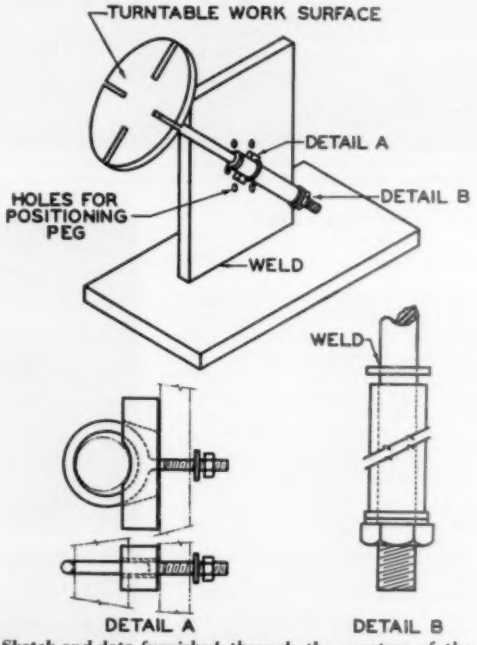
The term "slightly excess" is frequently seen in welding instructions. An operator may be directed to use either a "slightly excess" acetylene or a "slightly excess" oxygen flame to perform a certain job. In general, it means that it is better to have the flame on that particular side of the neutral flame rather than have it on the opposite. Thus, a "slightly excess" acetylene flame means one that contains a slight excess of acetylene to assure that no excess oxygen is present.

A Simple Positioner for Small Welding Jobs

THE welding positioner shown in the accompanying diagram can be assembled in a welding shop with scraps of steel plate, pipe and a few pieces of hardware. Size of the positioner will depend upon the size of the work generally performed in the shop.

The positioning table is slotted or drilled to accommodate hold-down bolts for the workpiece. To adjust the angle of the positioner, place the positioning peg in the hole in the vertical upright which will give the desired slant to the table. Loosen the nut holding the ring bolt, swivel the table and sleeve assembly until it rests on the positioning peg and tighten the ring bolt nut to hold this assembly in positioning.

Note that clamping is accomplished on the sleeve, thus leaving the shaft free to rotate. The lower end of the shaft is threaded and provided with a nut to control rotation of the table within the sleeve. When the nut is fully tightened, the table will not rotate; when the nut is loosened slightly, the table can be rotated as desired but will remain as positioned. A spring may be placed on the shaft between the nut and the sleeve to provide varying degrees of table rotation control.



Sketch and data furnished through the courtesy of the Air Reduction Sales Company.

Difficult Shipyard Jobs Speeded by Powder Cutting

CONSTRUCTION and repair can be speeded with powder cutting in the modern shipyard. Among the many difficult cutting jobs encountered are the cutting of stainless-steel door casings for ship boilers, the cutting and beveling of brass bars and bronze plates, the severing of cast-iron sections and the cutting of copper pipe into sections. The normal method has been either to attempt the job with a large oxyacetylene blowpipe, or to use arc cutting or to send the job to the machine shop.

Powder cutting is the easy, quick and economical way to perform these difficult cutting jobs in the shipyard. An iron powder is carried through a tube attached to an oxyacetylene blowpipe. The ignition of the iron powder provides the extra heat needed to cut through such metals as stainless steel, copper, aluminum, cast iron and various alloys. The powder-cutting blowpipe will pierce, bevel and cut most commercial metals, with very nearly the same speed and efficiency that an ordinary oxyacetylene blowpipe cuts through carbon steel.

In the accompanying illustration, powder cutting is being demonstrated for the cutting of stainless steel plate at Alabama Dry Dock and Shipbuilding Co., Mobile, Ala. A regular oxyacetylene blowpipe is being used. It has been equipped with a special powder-cutting attachment for this work. A small portable powder dispenser is in the background.

Photo and Data Courtesy The Linde Air Products Co.

An oxyacetylene blowpipe, equipped with a special powder-cutting attachment, is being used in the powder cutting of stainless steel plate at Alabama Dry Dock and Shipbuilding Co., Mobile, Ala.

Scrap Cutting Blowpipe Efficiently Operated on Gantry Rig

SLAG-ENCRUSTED skulls, spills and ladle buttons from the open-hearth operations at one steel mill presented a formidable problem in cutting for reuse as heavy-melting scrap. Oxygen lancing was slow and laborious. The problem was solved by the installation of an Oxweld C-60 Powder-Cutting Blowpipe, supplied by The Linde Air Products Co.

As shown in the photograph, a feature of this installation is the mounting of the cutting blowpipe, machine

The cutting blowpipe, mounted on a machine carriage and gantry rig, makes a convenient arrangement for the steel-mill scrap yard to reduce skulls and buttons to a size suitable for charging into the open-hearth furnace

carriage and track upon a gantry rig. The rig can be moved by electric motors to any position over the cutting bed.

The blowpipe is mounted on an arm which is attached to a machine carriage. The machine carriage moves the blowpipe over the work at a predetermined, but adjustable, rate of speed. The blowpipe can be raised or lowered as the cutting progresses, and can always be kept at a proper height above the surface of the skull.

Practice at this mill is to reduce the scrap material to the largest sections that can be conveniently charged into the open-hearth furnaces. By using large pieces of scrap in this fashion, and making fewer cuts, costs of scrap preparation are kept to a minimum. Skulls, spills or buttons of any size or composition can be cut by the C-60 blowpipe.

Some Unused Cover Pictures

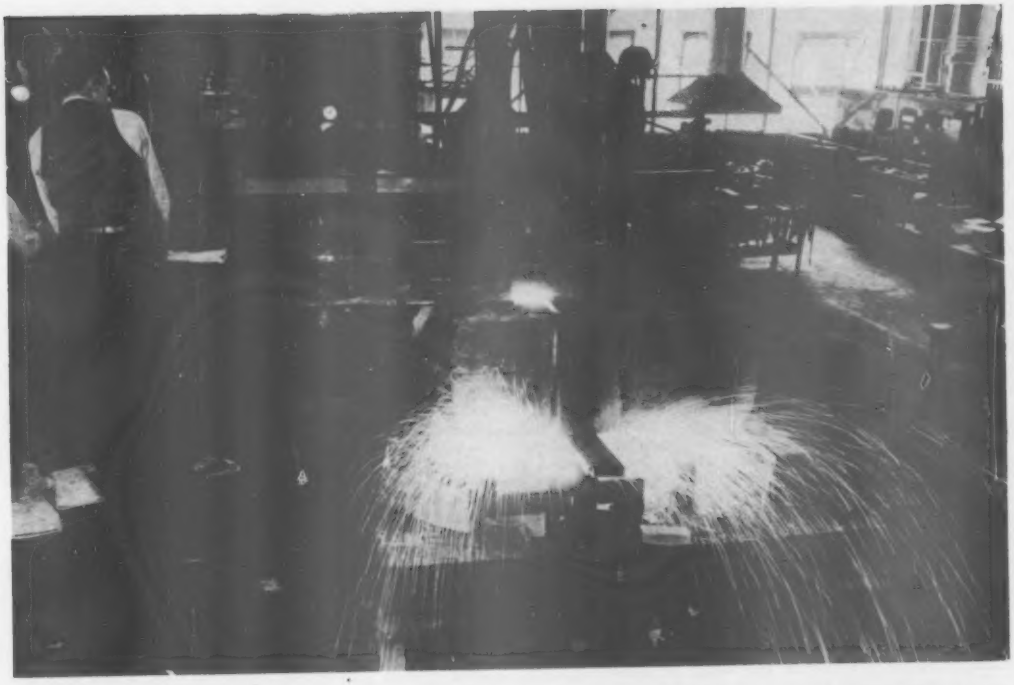
A change in cover format has left us with some interesting unused cover pictures, some of which we are reproducing herewith.

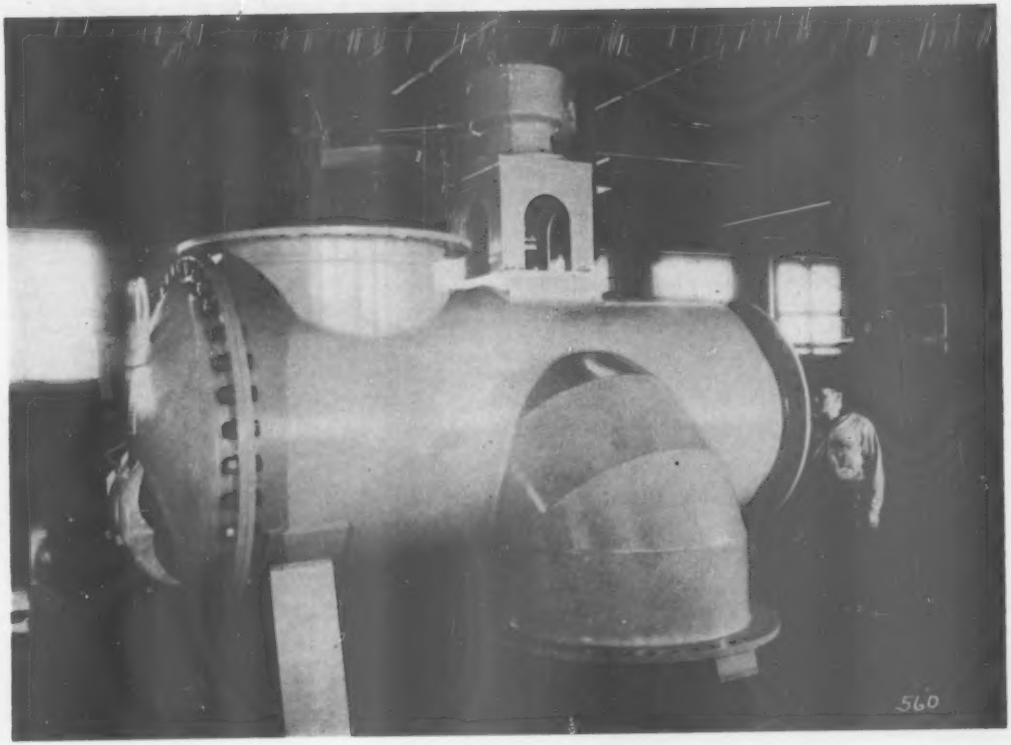
At the top of facing page is shown Oxygen shape cutting on Forging ranging from 24 to 48 inches in thickness, through the *courtesy of Northern Ordnance Incorporated.*

Directly below on the opposite page is shown a 48 in. Condenser reversing valve fabricated at the Chicago plant of the Henry Pratt Co.

Courtesy of C. F. Braun Co.

Welding pass-ribs in an exchanger. 6-pass channel.

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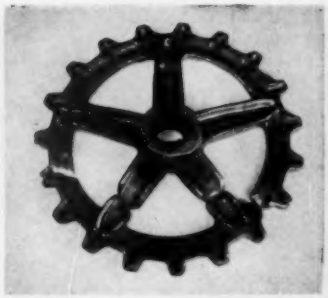

Fig. 1 Clean the broken edges. Remove the grease, rust and paint. Then bevel the broken edges but leave untouched sections to help line up the parts

Fig. 2 Adjust the flame for an excess of oxygen and warm the sprocket. Tin the edges and add weld metal. Braze weld the spokes on both sides

Fig. 3 Warm the sprocket again and weld both sides of one rim break. Then weld the other break. Build up welds higher than the surface

How to Bring Worn Parts Back to Life

by E. A. Shevey

SPROCKETS and gears are parts that get severe wear. Spokes and rims break and many times teeth are broken off. But repairs can be made quickly and easily with an oxyacetylene welding blowpipe and bronze welding rod.

With a blowpipe and bronze welding rod, you can return parts to service fast. Broken rims, spokes or hubs can be quickly repaired. Worn gear teeth can be built up, and after they are finished to their original size, the gear is ready to go back to work.

This sprocket is a good example of one of the simpler braze-welding repair jobs. It shows how a broken, discarded part can be taken from the scrap heap and "brought back to life."

The pictures and captions on this page show how to make the repair. Listed below you will find some general hints for all your braze-welding jobs. If you follow them and practice on old parts you will find that it is easy to do any repair by braze welding.

HINTS FOR BRAZE-WELDING

(*a*) Clean the weld areas thoroughly to remove grease, rust and dirt.

(*b*) Heat the part only to a dull red heat. Be sure it does not melt at any time.

(*c*) When the metal is at the proper temperature, dip the heated end of the rod in flux, melt the rod and form a small puddle.

(*d*) If the puddle is too hot the weld metal will boil and bubble. Draw the flame back for a few seconds. If the puddle is too cool, the molten bronze will form small balls and won't flow.

(*e*) Dip the rod into the flux often during welding.

(*f*) When adding molten rod metal to the puddle, place the end of the rod at the bottom of the puddle.

(*g*) Do not overheat the base metal at any time during the welding or the weld puddle will boil and there will be excessive fuming.

Fig. 4 When the welds are finished, let the sprocket cool slowly away from drafts. Excess weld metal need not be removed where it does not interfere with operation of the sprocket. It serves as extra reinforcement

E. A. Shevey, The Linde Air Products Co., Milwaukee, Wis.

the AMERICAN WELDING SOCIETY

activities • related events

The 33rd Annual Metal Show

The 33rd annual National Metal Congress & Exposition—the "Metal Show"—will be held in Detroit Mich., at the Michigan State Fair Grounds, Oct. 15–19, 1951. The announced floor space is 286,886 sq. ft.—6½ acres of exhibit area.

The Technical sessions will be held in the principal downtown hotels of Detroit.

Sponsoring the 1951 Metal Congress & Exposition in Detroit will be, in addition to the American Society for Metals, the AMERICAN WELDING SOCIETY; Metals Branch, American Institute of Mining and Metallurgical Engineers; and the Society for Nondestructive Testing.

W. H. Eisenman is the Managing Director and Chester L. Wells is the Assistant Director. Both are members of the American Society for Metals.

Headquarters for the National Metal Congress & Exposition is at 7301 Euclid Ave., Cleveland 3, Ohio.

Safety in Electric and Gas Welding and Cutting Operations, American Standard Z49.1—1950

The American War Standard on safety in welding and cutting proved so valuable in minimizing lost-time injuries and property losses, which might have occurred because of accelerated production rates and the use of untrained personnel, that it was decided to prepare an American Standard on the same subject. With industrial production on the increase again, issuance of the new American Standard *Safety in Electric and Gas Welding and Cutting Operations* is especially timely, containing 42 pages, and published by AMERICAN WELDING SOCIETY.

The present American Standard was prepared by a committee sponsored by the AMERICAN WELDING SOCIETY under the procedures of the American Standard Association. It reflects the best recommendations of welding engineers, safety engineers, equipment manufacturers, insurance organizations, labor organizations and governmental labor agencies, all of whom were represented on the Committee.

American Standard Z49.1 covers regulations for the safe installation and operation of welding equipment for all arc, gas and resistance welding processes. Provisions are included for fire prevention and protection in regularly assigned welding areas and in other locations as well. The protection of personnel is covered from the standpoint of the welder, for whom eye protection and clothing requirements, health protection, etc. are prescribed, and from the standpoint of other workers in areas adjacent to welding operations.

Precautions are specified for the welding of materials which may give off toxic fumes and for welding in confined spaces.

The ventilation requirements should be of particular interest. They have been considerably modified and more clearly prescribed than in the American War Standard and should be more useful to industry.

Under proper direction, use of *Safety in Electric and Gas Welding and Cutting Operations* should result in the elimination of possible hazards due to welding, hence the elimination of avoidable accidents and property damage—desirable in peace or war.

Copies of this Standard are available at 50 cents each from the AMERICAN WELDING SOCIETY, 33 W. 39th St., New York 18, N. Y., or American Standards Association, Inc., 70 E. 45th St., New York 17, N. Y.

Western Metal Congress and Exposition

With particular emphasis forced by the critical world situation and, particularly, forthcoming controls, the seventh Western Metal Congress and Exposition to be held in Oakland March 19th through 23rd will be the most important in history.

It will affect thousands of persons and attempt to answer problems important to the entire industry, the nation and the world.

Because of its extraordinary import, more than 100 committeemen are participating actively in arrangements. Last staged in Oakland in 1947, the Exposition, one of the largest trade shows of its kind, will be even larger and more vital in this critical period, according to William H. Eisenman, secretary of the American Society for Metals, and general manager of the great show and congress.

Its theme, "Production for America," will be divided into two phases—production for defense or rearmament, and production for civilian or nonemergency use.

The haste being urged toward preparedness makes the metal industry, with its many facets and allied industries, particularly cognizant of the need for speeding up production.

Principal object of the five-day Oakland meeting and show, which is expected to draw in excess of 25,000 persons, will be to devise the means for achieving the maximum increase in production in the minimum period of time at the most reasonable cost. All new methods to be demonstrated will prove of particular significance and advantage to main and subcontractors who are likely to be involved in new restrictions. Eisenman reports that space for exhibitors in the exposition, to be held in Oakland's huge municipal auditorium and adjacent exposition hall, is going fast although some remains.

The need for conservation of strategic materials and substitution of others needed less urgently will be stressed not only by all speakers at the Congress but by all distributors in the Exposition halls. It has been indicated to what extent industry will be kept from having a free hand in obtaining usual materials for civilian production.

Yet, the need for keeping civilian production as high as possible without interfering with the rearmament program will be discussed and planned.

With 64,000,000 Americans employed industrially, speakers and exhibitors will give thought toward stabilization of this tremendous force.

Top-flight experts of the world will be in Oakland during the technical sessions, heralded as the most timely and important in the metal industry's history. Eiseman has established offices in Oakland's Exposition Bldg., 918 Fallon St., to assign the remaining exposition space and to assist committees in completing plans for the important meeting.

The great Congress and Exposition is sponsored by the American Society for Metals, with the cooperation of the Western Sections of the American Society of Tool Engineers, the AMERICAN WELDING SOCIETY, the Pacific Coast Gas Association, the American Institute of Chemical Engineers, the American Society for Testing Materials, the American Society for Mechanical Engineers, the American Society of Civil Engineers, the American Institute of Electrical Engineers, the National Association of Corrosion Engineers, the American Foundrymen's Society, the Institute of Aeronautical Sciences, the Purchasing Agents Association of Northern California, the American Chemical Society, the American Institute of Mining and Metallurgical Engineers, the Mining Association of California, the National Association of Lubrication Engineers and the Society of Automotive Engineers.

Western Metal Congress, Western Metal Exposition, March 19-23, 1951

AMERICAN WELDING SOCIETY Program Schedule

Monday, March 19, 1951

10:00–11:00 A.M.—"Bridge Structural Welding," by L. C. Hollister, Bridge Engineer, Division California State Highways.

11:00–12:00 A.M.—"Structural Welding," by La Motte Grover, Welding Engineer, Air Reduction Sales Co.

12:15 A.M.–1:30 P.M.— AMERICAN WELDING SOCIETY Official Convention Luncheon.

2:00–3:00 P.M.—"Quality Control of Structural Resistance Welding in Aircraft," by J. R. Fullerton, Welding Engineer, Ryan Aircraft Co.

3:00–4:00 P.M.—"Recent Developments in Brazing and Welding of Copper," by James P. Kemp, Metallurgical Engineer, American Brass Co.

Tuesday, March 20, 1951

10:00–11:00 A.M.—"The Welding and Metallurgical Phase of Large Diameter High-Strength Cold-Worked Pipe and Some Interesting Notes Relative to Its Testing," by W. A. Saylor, Metallurgist, Consolidated Western Steel Corp.

11:00–12:00 A.M.—"Recent Developments in Flame Cutting," by T. T. Parker, Supt. of Specialty Mfg., Kaiser Steel Corp.

2:00–3:00 P.M.—"Automatic Hard Facing," by T. R. Brashear, Supt. of Automatic Welding, Leader Welding & Mfg. Co.

3:00–4:00 P.M.—"Shielded-Arc and Submerged-Arc Welding as Used in the Centrifugal Pump Industry," by F. R. Drayhos, Metallurgist, Byron Jackson Co.

4:00–5:00 P.M.—"Applied Welding Engineering, the Proper Application of Welding Processes, Introduction, Design, Materials, Workmanship, Inspection and Applications," by Gilbert S. Schaller, University of Washington.

Wednesday, March 21, 1951

10:00–11:00 A.M.—"Welding Jet Component Parts," by G. L. Richardson, Manager Product Service, General Electric Co.

11:00–12:00 A.M.—"Semiautomatic Inert-Gas Metal-Arc Welding," by L. Robbins, Welding Engineer, Mare Island Naval Shipyard.

2:00–3:00 P.M.—"Fundamentals of Inert-Gas-Shielded-Arc Welding," by H. E. Rockefeller, Asst. Manager of Development & Process Service, The Linde Air Products Div., Union Carbide and Carbon Corp.

3:00–4:00 P.M.—"Advancement in the Aircomatic Welding of Copper-Base Alloys and Other Metals," by J. H. Berryman, Assistant Manager, General Technical Sales Div., Air Reduction Sales Co., Inc.

4:00–5:00 P.M.—"Welding of Stainless Steel and Its Effect on Corrosion," by E. W. Hopper, Crucible Steel Co.

Thursday, March 22, 1951

2:00–3:00 P.M.—"Flash Welding of Mild-Steel and High-Strength Alloys for High-Speed Production," by Chas. Smith, Engineer, Douglas Aircraft Co.

3:00–4:00 P.M.—"Kaiser Steel 14 In. Electric Weld Process," by R. G. Reigel, Supt. of Electric Welded Pipe, Kaiser Steel Corp.

4:00–5:00 P.M.—Movie on "This is Resistance Welding," by General Electric Co.

Friday, March 23, 1951

9:00–10:00 A.M.—"Progress Report on Welding Processes," by T. B. Jefferson, Editor, *The Welding Engineer.*

10:00–11:00 A.M.—"Should Welding Be on Its Own in Education?" by R. C. Wiley, California State Polytechnic College.

11:00–12:00 A.M.—Open Discussion—Subject: "Education of the Welding Engineer."

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175	325	650	1200	1750
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NEWS OF THE INDUSTRY

Inert-Arc Welding on A.S.T.E. Meeting Agenda

One of the important production know-how papers to be featured at the American Society of Tool Engineers annual meeting in New York, March 15th to 17th inclusive, will be an intense discussion of inert-gas-shielded arc welding as a manufacturing process.

The subject is expected to become of increasing importance due to the nature of armament production.

Other papers will cover a variety of subjects of interest to users and producers of welding equipment. They range from a discussion of the economics of machine replacement, to a symposium on non-destructive testing of materials.

Pullman Inc. Expands

For the information of stockholders, arrangements have just been completed by one of the Corporation's subsidiaries, Pullman-Standard Car Mfg. Co., to enter the tractor allied-equipment field.

On Dec. 29, 1950, Pullman-Standard will acquire the entire tractor allied-equipment business of Isaacson Iron Works at a cash price to be finally determined later on in accordance with the recently executed closing agreement.

While the Isaacson tractor allied-equipment business is not large in relation to Pullman-Standard's present volume of railway car business, its acquisition is considered to be an attractive opportunity to enter a new field where growth potentialities are clearly apparent and for which Pullman-Standard's facilities and resources are well adapted.

The term "tractor allied equipment" refers to various accessory products such as bulldozers, scrapers, rippers, hydraulic power units, hauling winches and other equipment utilized with crawler-type tractors, primarily in the earth-moving field.

These Isaacson products, now being manufactured at plants in Seattle, Washington and Rockford, Ill., are sold in competition with tractor allied equipment of other makes throughout the United States by the same distributors who handle the marketing of International Harvester crawler-type tractors, with which Isaacson allied equipment is used. Foreign sales of such Isaacson products are handled through International Harvester Export Co.

The phase of this tractor allied-parts activity now carried on at Isaacson's Seattle plant will be transferred as soon as possible to the Hammond, Indiana plant of Pullman-Standard. Operation of the Isaacson plant at Rockford, Ill., will be continued by Pullman-Standard.

Airco Plant to Be Built at Butler, Pa.

Purchase of a site for the erection of another link in its chain of plants throughout the country for the manufacture of oxygen and nitrogen, together with related facilities, has been announced by H. R. Salisbury, President of Air Reduction Sales Co., A Division of Air Reduction Co., Inc.

The new plant and facilities, designed by Air Reduction and to be constructed by Koppers Company of Pittsburgh, Pa., will occupy a tract of land covering about 23 acres in Butler, Pa. The new plant is expected to be in operation the latter part of 1951.

This new unit, capable of producing very large quantities of oxygen per day, both liquid and gaseous, will also produce liquid and gaseous nitrogen and other gases extracted from the air. Most of its production will be used to fabricate materials required for the expanding defense effort.

Amongst the large local users who will be supplied from this production unit will be Armco Steel Corp. and Pullman Standard Car Mfg. Co. In addition, facilities will be available to supply oxygen and nitrogen in either gaseous or liquid form throughout western Pennsylvania.

Approximately thirty employees will perform round-the-clock, seven-day operations.

Symposium on Plasticity and Creep of Metals

This symposium covers four outstanding phases of this interesting subject—the plastic deformation and flow of metals—presented at the First Pacific Area National Meeting of A.S.T.M. in 1949; and provides in compact form a considerable amount of pertinent information and data.

In the Introduction, J. E. Dorn, Professor of Metallurgy, University of California, points out that engineering interest in the plastic deformation and flow of metals originated during prehistoric times when our ancestors began to hammer and forge metals into shape. During the nineteenth century, industrial activities, in forming metals and in the application of metals to elevated temperature service, stimulated not only the art of metal forming but also the science of plasticity of metals. Extensive investigations into these fields were sponsored by the various industries, governmental agencies and the armed forces over 1941 to 1945; and currently other work continues.

The papers and their authors are: "The Experimental Exploration of Plastic Flow in Sheet Metals," L. R. Jackson and W. T. Lankford, Battelle Memorial Institute; "Forming Parameters and Criteria for Design and Production," William Schroeder, Lockheed Aircraft Corp.; "The Use of Creep Data in Design," H. C. Cross and L. R. Jackson, Battelle Memorial Institute; "Super Creep-resistant Alloys," J. W. Freeman, D. N. Frey, E. E. Reynolds and A. E. White, University of Michigan.

Bibliographies and discussions enhance the value of this publication which should be of much interest to all metallurgists, design engineers and those fabricating parts and equipment where plastic flow and the effect of temperature are important considerations.

Copies of this 72-page symposium (STP 107), in heavy paper cover, can be obtained from American Society for Testing Materials, 1916 Race St., Philadelphia 3, Pa., at $1.50 each.

Tube Turns Distributors

John G. Seiler, executive vice-president and general sales manager of Tube Turns, Inc., has announced the appointment of the Bethlehem Supply Co., and the Bethlehem Supply Co. of California as distributors of Tube-Turn welding fittings and flanges.

The Bethlehem Supply Co. is a subsidiary of the Bethlehem Steel Co. and has its main offices in Tulsa. Its oil country stores and service organization cover Arkansas, Colorado, Illinois, Kansas, Louisiana, Mississippi, New Mexico, Oklahoma, Texas and Wyoming. G. A. Tompson is president and C. R. Zimmerman is executive vice-president.

The Bethlehem Supply Co. of California is a subsidiary of the Bethlehem Pacific Coast Steel Corp. Its main offices are in Los Angeles and its oil country stores and service organization are located in eleven strategic areas in California. Wendell M. Jones is vice-president and general manager and J. W. Hollingsworth is general manager of sales.

Tube Turns, Inc. is the nation's oldest and largest manufacturer of seamless welding fittings for industrial piping.

New Aircomatic Movie—"The Tool for the Job"

The Air Reduction Sales Co., a division of Air Reduction Co., Inc., has announced the completion of a Kodachrome, sound motion picture which tells the story of the new aircomatic process for welding aluminum, stainless steel, bronzes and Monel.

This film, in an expository manner, ex-

(*Continued on page 188*)

EMPLOYMENT OPPORTUNITY FOR ENGINEER

AWS is seeking an engineer to join Headquarters' Technical Staff as Assistant to the Technical Secretary.

Here are the duties—

Working with AWS technical committees (see page 29 Yearbook) in gathering and correlating technical data; preparing minutes, reports and other records of committee activities; editing standards for publication as completed.

Organizing and correlating related activities for most efficient progress.

Disseminating technical information in answer to inquiries from industry.

Related duties as assigned.

Here are the qualifications—

Ability to work with technical men at all levels in a cooperative manner. This is necessary to successfully deal with committee personnel, AWS members and the general public.

A degree in engineering, any branch, preferably with a record of participation in extra-curricular student activities.

At least three years experience in engineering work, preferably where working to standards and specifications was involved.

Here is an opportunity—

The man we are seeking has initiative and imagination and is interested in becoming familiar with welding in all its aspects and for every type of application. Salary will be commensurate with qualifications and experience; promotion will be commensurate with demonstrated ability.

If interested in being interviewed for this position, in your own or a nearby city, send a resume* of your education, experience and personal background to

Mr. S. A. Greenberg,
Technical Secretary
American Welding Society
33 West 39th Street
New York 18, N. Y.

* Will be held confidential.

Announcing

The EUTECTIC WELDING ALLOYS CORPORATION

$1000.00 PRIZE COMPETITION

To Welding Engineers, Researchers, Metallurgists, Instructors, University Students and all others qualified . . .

for papers on

Technological and Research Aspects, Advances and Advantages of the Use of Lower Melting (lower than parent) Filler Metals in the Non-fusion Welding Processes.

R. D. Wasserman
President

AWARD FUND

FIRST PRIZE $500.00
SECOND PRIZE 300.00
THIRD PRIZE 200.00

Rules of the Competition

(1) The competition is open to all persons qualified to present basic principle, theory or the results of practical research and experience in the field of non-fusion "lower melting" (lower than parent) filler metals in welding, except employees of Eutectic Welding Alloys Corp., its advertising agencies, and members of their families.

(2) Participants in the competition may submit welding papers on one or more of the following subjects:

(a) Oxy-acetylene, low melting filler
(b) Oxy-fuel gas, low melting filler
(c) Brazing and bronze welding
(d) Hard facing and resurfacing, with a low melting filler

The welding application may be by torch, furnace induction, carbon arc or electric arc.

(3) Papers are to be presented in English and should be typewritten.

(4) Papers should be confined specifically to the field defined above and may be documented with photographs, test data or other material to substantiate and verify the thesis presented or described.

(5) All papers must be the original, previously unpublished work of the participant and must bear his name and address.

(6) Papers will be judged by The Jury and Committee of Awards on the basis of the merit of the theory or practice presented. Decision of the judges will be final. All entries and ideas therein become the property of Eutectic Welding Alloys Corporation and none will be returned.

(7) The competition opens Sept. 1, 1950 and closes May 31, 1951 midnight. All entries must be postmarked on or before that date.

(8) Winners of awards will be notified by mail within approximately 60 days after the close of the competition. Complete list of winners will be sent to all participants requesting it.

THE JURY and COMMITTEE of AWARDS

Dr. Th. I. Leston, Vice President, Eutectic Welding Alloys Corporation
Prof. Otto H. Henry, Dept. of Metallurgy, Polytechnic Institute of Brooklyn
Dr. Robert Humphrey, Vice Chairman, American Society for Metals

Papers should be directed to:

Welding Research Competition
EUTECTIC WELDING ALLOYS CORPORATION
40 Worth Street, New York 13, N. Y.

WELDING

CODES STANDARDS and SPECIFICATIONS

Are you fully informed on the latest welding standards available? Have you missed any of the earlier standards? This column is published as a regularly monthly feature of The Welding Journal to keep you abreast of AWS technical standards, which are universally recognized as the most authoritative source of welding information.

The list of publications shown below is only partial; it is changed from month to month.

Keep informed—read this column regularly

D. INDUSTRIAL APPLICATIONS

Buildings, Bridges, Tanks, Aircraft, Automotive, Pipe, Ships

D1.0-46 Standard Code for Arc and Gas Welding in Building Construction	**$1.00**
D2.0-47 Standard Specifications for Welded Highway and Railway Bridges	**$1.00**
D3.1-41 Report on Thermal Stresses and Shrinkage in Welded Ship Construction	**25c**
D3.2-44T Structural Failures in Welded Ship Construction (Tentative)	**25c**
D3.3-48 Rules for Welding Piping in Marine Construction—Carbon Steel Only	**25c**
D4.0-44T Weldability Standards for Alternate Aircraft Steels (Tentative)	**25c**
D5.1-47 Rules for Field Welding of Steel Storage Tanks	**50c**
***D5.2-48 A.W.W.A.-A.W.S. Standard Specifications for Elevated Steel Water Tanks, Standpipes and Reservoirs**	**30c**
***D7.0-49T A.W.W.A.-A.W.S. Standard Specifications for Field Welding of Steel Water Pipe Joints (Tentative)**	**15c**
D8.1-46T Recommended Practices for Automotive Flash Butt Welding (Tentative)	**50c**
D8.2-48T Survey of Automatic Arc and Gas Welding Processes as Used in the Automotive Industry	**30c**
D8.3-50T Recommended Practices for Salvaging Automotive Gray Iron Castings by Welding	**50c**
* Binder for All Codes and Standards in Groups A, B, C & D. Members / Non-Members	$3.00 / $3.50

NOTE: 25% discount to A and B members and 15% discount to C members of AWS on copies of any codes and standards listed above except starred items.

Send your orders, or requests for order forms containing complete list of AWS publications to:

Dept. T,

American Welding Society

33 West 39th Street New York 18, N. Y.

INDEX TO ADVERTISERS

Air Reduction Sales Co.	Outside back cover
All-State Welding Alloys, Inc.	202
The American Brass Company	Inside back cover
Aluminum Company of America	206
American Chain and Cable Co.	187
American Manganese Steel Division	195
Anti-Borax Compound Co., Inc.	188
Arcos Corporation	207
Bastian-Blessing Co.	208
The Burdett Oxygen Company	193
The Champion Rivet Company	192
Colonial Tanning Co., Inc.	209
Eastman Kodak Company	205
Eutectic Welding Alloys Corporation	185
General Electric Co.	183
Harnischfeger Corporation	199
Hobart Brothers Company	97
The International Nickel Co., Inc.	102
The Lincoln Electric Company	189
The Linde Air Products Company Unit of Union Carbide and Carbon Corporation	100
P. R. Mallory & Co., Inc.	99
Metal & Thermit Corporation	98
National Carbide Company	194
National Cylinder Gas Co.	196 & 197
Page Steel and Wire Division	187
The Reid-Avery Company	191
Sciaky Bros., Inc.	201
Tempil° Corp.	182
Titanium Pigment Corporation	203
Tube Turns, Inc.	213 & 214
Tweco Products Company	210
Union Carbide and Carbon Corp., The Linde Air Products Company, Unit	100
Victor Equipment Company	Inside front cover
Westinghouse Electric Corporation	188
J. H. Williams & Co.	188

PAGE *Welding* ELECTRODES

Page-Allegheny *Stainless Steel*

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ARC
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PAGE STEEL AND WIRE DIVISION
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TRADE MARK

Get in touch with your PAGE Distributor

(*Continued from page 84*)

plains the nature, character and uses of the aircomatic process. One of its outstanding features is the exceptional close-up photography of the aircomatic arc. Through the use of special filters and camera techniques, actual deposition of weld metal and much detail in the arc are brought to the screen. Other aspects of the process are explained by means of animation and demonstration.

The manual and automatic welding of such metals as aluminum, stainless steel, bronze and Monel are covered in some detail both in demonstration and laboratory sequences as well as in actual production work.

Among users of this process who cooperated in the production of this film were A. J. Fritschy Corp. of Brooklyn, N. Y. and the Martin Quaid Co. of Philadelphia, Pa.

This film was premiered in the Economy Theater at the National Metal Congress held in the International Amphitheater, Chicago, Ill., Oct. 23–27, 1950.

Showing time for this 16 mm., two reel, Kodachrome, sound motion picture is 21 min. This film was especially designed for showing to nontechnical as well as technical groups, and is suitable for presentation before such varied groups as Purchasing Agent's Associations, Foremen's meetings, Trade Schools and Colleges, Sales and Engineering meetings and Technical Society meetings. For further details, and to make arrangements for showing this film, please communicate with your nearest Airco office or write to Air Reduction, 60 E. 42nd St., New York 17, N. Y. We suggest that you make bookings well in advance to insure availability.

A. B. King Expansion

A plant expansion and consolidation program of the Alfred B. King Co., with its subsidiaries, Churchward Welding Accessories and KIF Industrial Fabricators, was recently completed with the moving of these companies to their new 10,000-sq. ft. plant in North Haven, Conn. It is located at the foot of Devine St., adjacent to the New Haven Railroad and through Truck Route No. 5.

A modern 2200-sq. ft. office building and a 3000-sq. ft. warehouse are included with the new plant which is located on a six-acre tract to allow room for further expansion. All buildings were constructed to the Alfred B. King Co.'s specifications, including installation of the latest equipment for materials handling to streamline the manufacture of its own materials handling products.

Enlargement of the Materials Handling Mfg. Div. is still under way. Further new additions will be made as soon as weather permits.

Coincident with the move and expansion, the company has augmented its designing staff and incorporated it in the engineering department.

Fabrication of special materials handling equipment and stainless steel items for meat packing, textile and general manufacturing industries has already begun in the plant. The company also fabricates steel and alloys to buyer's specifications.

Formerly of 200 Winchester Ave., New Haven, Conn., the Alfred B. King Co., which was established in 1925, is a distributor for nationally known cranes, electric trucks and conveyors. Its recently organized subsidiaries, Churchward Welding Accessories and KIF Industrial Fabricators, are producing patented items for use in welding and materials handling.

WELDING CONNECTORS
Saxe System Welded Connection Units
for welded assembly

Saxe Units place in position and securely hold together structural parts to be welded.

As used in many welded structures they *eliminate all hole punching* producing an economical, rigid, safe and quickly erected structural frame.

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No. 9 Stainless Steel Welding Flux
No. 11 Tinning Compound
No. 16 Silver Solder Paste Flux

Mfg. By
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Fort Wayne, Ind.

D-C WELDER SAVINGS PROVED AGAIN IN THE FIELD

Take the case of the 2,000,000-gallon water tank erected by the Pittsburgh-Des Moines Steel Company at Niagara Falls, New York. A battery of D-C Rectifier Welders fed by one large diesel generator replaced a number of diesel-driven welding generators formerly used.

Maintenance was reduced 85%—weight was slashed by 15,000 pounds—initial cost was cut 50%—fuel savings totaled $8.70 a day!

For your welding application—large or small—call your nearest Westinghouse Representative or write Westinghouse Electric Corporation, Welding Division, Buffalo, New York.
J-21605

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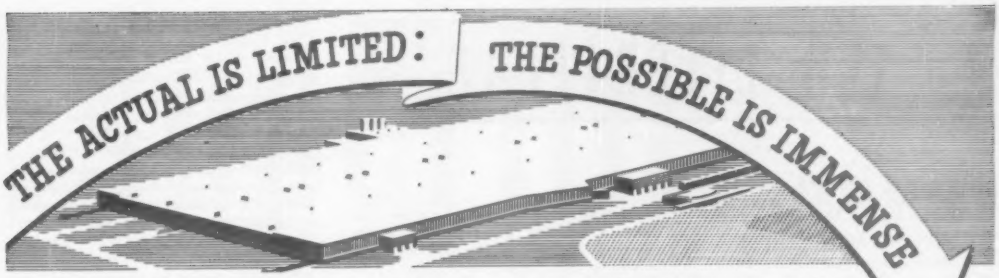
D-C Welders



NEW LINCOLN PLANT CREATED BY INCENTIVE-INSPIRED CO-ACTION IN DEVELOPING POSSIBILITIES IN PRODUCT

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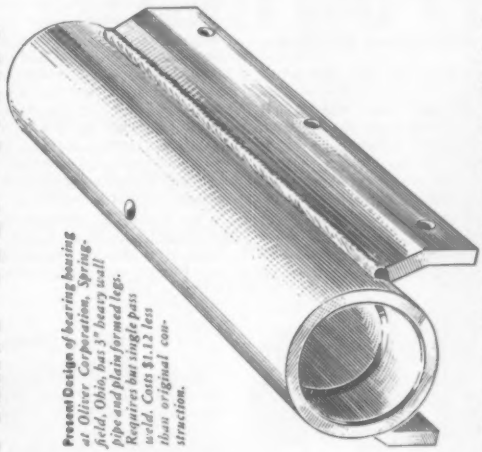
MANUFACTURER CUTS TOOLING COST $800.00

BY simple redesign to welded steel, substantial savings are being effected in the production of these bearing housings. At the same time, product strength and rigidity have been increased to assure maximum serviceability . . . long-lived operation. An initial saving of $800.00 on setup and tooling expense results from the elimination of drill jigs and boring fixtures as well as for patterns and core boxes required with the original construction.

Component parts are now preformed prior to welded assembly in a plain, low-cost, clamp type fixture. The tubing itself is bored as a simple lathe operation and the legs prepunched and bent as shown.

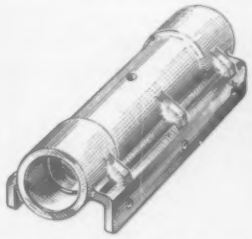
The resulting simplified production now possible with the welded steel construction is increasing the rate of output . . . reducing unit cost $1.12 per piece.



Present Design *of bearing housing at Oliver Corporation, Springfield, Ohio, has 3" heavy wall pipe and plain formed legs. Required but single pass weld. Costs $1.12 less than original construction.*

How To DESIGN FOR WELDED STEEL is presented in the new 9th Edition "Procedure Handbook of Arc Welding Design and Practice." Contains latest data on machine design together with cost figures. Price only $2.00 postpaid in U.S.A.; $2.50 elsewhere.

the ACTUAL



Original Cast Construction *of bearing housing required fixtures for extensive boring and drilling operations.*

increasing the YIELD

First Changeover *to welded steel design has 90° formed legs welded to thin wall tubing. Was welded top and bottom as shown.*

the IMMENSITY of the POSSIBLE

A saving in cost of 24%

SEE HOW WELDED STEEL DESIGN SIMPLIFIES PRODUCTION

Machine Design Sheets free on request to designers and engineers. Write on your letterhead to Dept. 92,

THE LINCOLN ELECTRIC COMPANY

CLEVELAND 1, OHIO

NEW LITERATURE

NEMA Color Markings for Electrode Identification, Publication No. EW2—1950

The identification by color of all arc-welding electrodes for which a standard identification has been established is covered by this standard which supersedes Publication No. 45-108. ($0.25)

Argon Metal-Arc Welding

A new booklet, "Procedures and Equipment for Argon Metal-Arc Welding," describes the apparatus for this new welding method, which uses argon gas to shield the consumable filler-metal electrode and the welding area. The features, work range and photographs of Argon Metal-Arc welding equipment are included in this 6-page booklet.

The booklet can be obtained without charge from the nearest office of The Linde Air Products Co., or by writing direct to The Linde Air Products Co., 30 E. 42nd St., New York 17, N. Y.

Pipefitter Welder's Review of Metallic-Arc Welding

In this pamphlet, "Pipefitter Welder's Review of Metallic-Arc Welding," for qualification under A.S.M.E. code rules, by Robert S. Green, Consultant, National Certified Pipe Welding Bureau, Prof. Green has given the requirements of the qualification tests prescribed by the A.S.M.E. Boiler Construction Code in simple language and clear line drawings. There is no mystery, no long technical statements nor complicated statements to bewilder the reader.

How to make a good fusion weld by the metallic-arc method is told, step by step, from the preparation of the stock to the placing of the last bead.

The purpose of the pamphlet is to give the pipefitter welder, facing the test, confidence in his ability to pass it, by reviewing with him the basic steps in obtaining a good weld by this method.

Copies of the pamphlet can be obtained from the National Certified Pipe Welding Bureau, Suite 1401, 1250 Avenue of the Americas, New York 20, N. Y., for 50 cents per copy.

Welding Electrode Comparison Chart

The new Comparison Chart, published by The Champion Rivet Co., Harvard Ave. & East 108th St., Cleveland 5, Ohio is considerably expanded over a previous issue, containing many special-purpose electrodes as well as the mild steel and low-alloy high-tensile electrodes. In its folded form it is easy to use as a pocket guide, and unfolded may be used as a wall chart.

Welding with Bronze Electrodes

A 24-page reference and instruction book, *Welding with Ampco Bronze Electrodes* has recently been released for distribution. The contents include technical and pertinent information about every bronze electrode which Ampco Metal, Inc., manufactures along with recommended welding techniques, welding procedures and machining suggestions.

Convenient charts covering the selection and preheating of bronze electrodes and the weldability of Ampco electrodes are also included.

Free copies of this book (Bulletin W-17) can be obtained by writing to Ampco Metal, Inc., 1745 S. 38 St., Milwaukee 46, Wis.

Welded Deck Highway Bridges

Welded Deck Highway Bridges, edited by James G. Clark, Professor of Civil Engineering, University of Illinois and published by The James F. Lincoln Arc Welding Foundation, Cleveland 1, Ohio.

It contains 247 pages; design details and information from designs of welded deck highway bridges submitted in *Welded Bridges of the Future*, 1949 award program; over 100 drawings of bridges and details clearly reproduced on excellent paper; bound in stiff board with cloth covering, gold-embossed. Price $2.00 in U.S.A., $2.50 elsewhere, postage prepaid.

Welded Deck Highway Bridges presents the thinking of some of the world's foremost bridge designers and fabricators on the subject of what is the best way to make an all-welded 120-ft. deck highway bridge. Their forward-looking thinking on design, fabricating and erecting, steel sections and other details will be of great value to all persons interested in steel structures, both as a source of design ideas as well as practical shop and field suggestions.

The James F. Lincoln Arc Welding Foundation of Cleveland, Ohio is publishing this book at a nominal cost to make generally available the large amount of significant information and the new ideas on welded bridge design contained in the designs submitted in the 1949 *Welding Bridges of the Future* award program. The designs have been edited and the book written by James G. Clark, Professor of Civil Engineering at the University of Illinois. Clark also served on the Jury of Award for the program.

In his introduction Clark says of his book, "While it would be expected that all engineers should not agree as to what the best design should contain, and that an individual designer in his design may create some expensive details while eliminating other more expensive details, it is bound to be true that the publication of some of the excellent ideas presented in this program will benefit the field of bridge engineering and enable many designers to add this information to that which they already have."

The 1949 award program offered awards for the best designs of an all-welded 120-ft. deck highway bridge. The program emphasized developing new designs, new methods of fabricating and erecting new steel sections, all of which will result in better bridges at less cost made with less steel. Selection of details and designs from the program for discussion in the book was based on the intention of discussing enough designs to insure that most of the new and important ideas presented in the program would be included.

The book is divided into the following chapters: Structural Types, Floor Systems, New Sections, Special Connections and Details, and Quantities and Costs.

Welding Fluxes

Developed to meet the constantly increasing demand for more efficient chemical aids to welding, an expanded line of welding fluxes, known as "Lo-Cost Flotectic Fluxes," has been released by Eutectic Welding Alloys Corp., 40 Worth St., New York, N. Y.

Included in the new flux line is "Flotectic Flux 1100," claimed to be capable of drastically reducing silver solder costs by cutting down on the amount used per weld; as well as speeding up production by insuring rapid flow with maximum capillary action.

Complete details of these new products, together with data on application ranges, suggested metals use, governmental specifications and other helpful information, appear in a new Bulletin No. 106P available upon request from the manufacturers. Please mention the name of this publication when writing.

Repair of Worn Buckets

Particularly timely is an informative folder describing the rebuilding and repairing of worn buckets quickly and inexpensively with Manganal 11 to 13½% Manganese-Nickel steel products. This illustrated folder is available upon request from The Stuls-Sickles Co., 134 Lafayette St., Newark 5, N. J.

Worn buckets, ordinarily discarded or repaired only by expensive methods, can now be salvaged and put back on the job inexpensively with Stuls-Sickles' patented manganal products. Manganal, a special-

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You can also salvage many other worn parts at a minimum cost with Manganal —hammermill hammers, truck bottoms, crusher parts, tractor sprockets and pads, shovel and drag-line bucket teeth, conveyor chain wear-strips and chutes, for example.

Car Shaker Bulletin

Allis-Chalmers car shaker for unloading granular material from hopper-bottom gondola cars is described in a new bulletin released by the company.

Construction features of the shaker are given along with specifications and a cross section through the vibrating mechanism.

The shaker, designed to save time and money and to eliminate danger to operating personnel, has applications in power houses, steel mills, sand and gravel, chemical, coke and glass plants, coal mines and docks, sugar beet and paper mills, foundries and building block and slag product manufacturing plants.

Copies of the bulletin, "Allis-Chalmers Car Shaker," 07B7221A, are available upon request from Allis-Chalmers Mfg. Co., 1023 S. 70th St., Milwaukee, Wis.

The Chemical Formulary

Thousands of industrial formulas and methods of their formulation are listed in this new volume of *The Chemical Formulary*, Vol. IX, H. Bennett, Editor-in-Chief.

Every formula included in this volume is different from those of the previous volumes and it represents the newest developments in each field covered.

The formulas as they are may be used for preparing the basic products of a successful manufacturing firm and they may also serve as a source of ideas and a starting point of experimentation for formulating different products.

No matter how simple or complex your problem may be, this book will enable you to solve it easily and quickly.

The alphabetical list of chemicals and trade-mark products with the names of their manufacturers will serve as a guide in buying the chemicals for your formulations. Price $7.00.

Chemical Publishing Co., Inc., Brooklyn 2, N. Y.

Standard Abbreviations

A revised edition of the American Standard Abbreviations for Use on Drawings has just been published by the American Standards Association, 70 E. 45 St., New York, N. Y.

To bring the 1946 edition of the standard up to date with latest industry-wide practice a committee representing technical associations, industry, government and independent experts developed this edition. It will greatly aid draftsmen, shopmen, assemblers and construction men in interpreting industrial drawings done by various companies and branches of the government.

Over 200 changes have been made in the abbreviations included in the original edition, and more than 40 new abbreviations have now been incorporated.

The committee developing this standard was jointly sponsored by the American Institute of Electrical Engineers and the American Society of Mechanical Engineers under the procedures of the American Standards Association.

Copies of the American Standard Abbreviations for Use on Drawings, Z32. 13–1950, may be obtained from the American Standards Assn., 70 E. 45 St., New York 17, N. Y. at $1.00 per copy.

New Welding Alloys Chart

Containing complete specifications on close to 100 different Eutectic low-temperature welding alloys used in welding, brazing and hard surfacing of steel, alloy steels, stainless, cast iron, brass, bronze, copper, aluminum, magnesium, zinc die

LIL CHAMP
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WEAR DEVIL A IS A HEAVY FLUX COATED ELECTRODE DEPOSITING WELDS WITH NO TENDENCY TO CRACK OR CHECK. ALSO...
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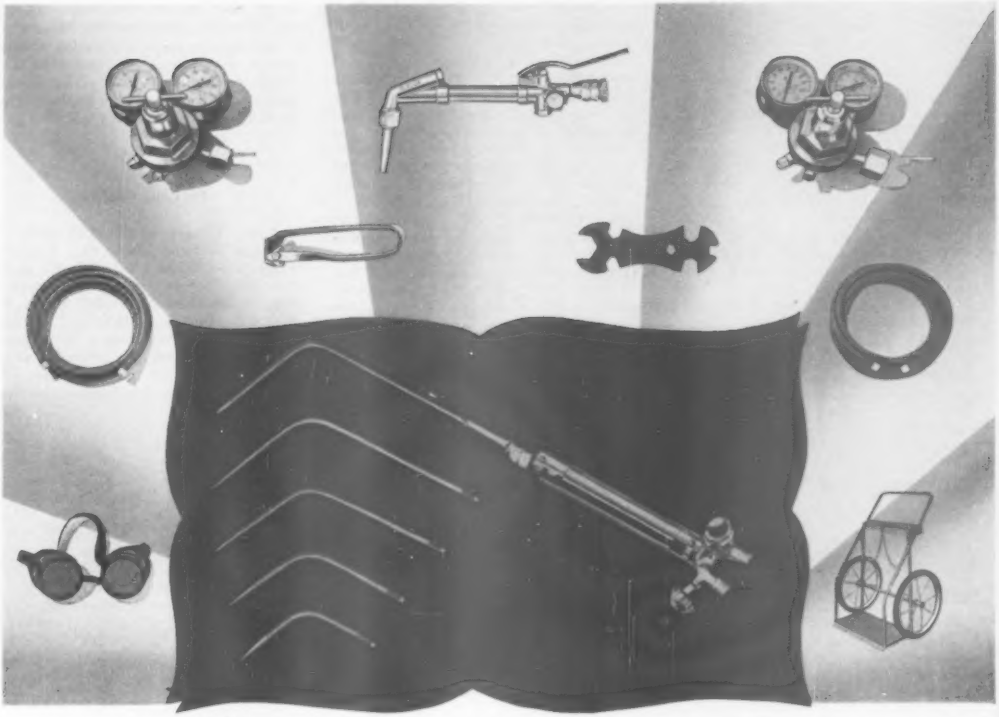

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WELDING ELECTRODES • CYLINDER TRUCKS • AND MANY OTHERS

cast, etc., a new reference chart is now available without charge from Eutectic Welding Alloys Corp., 40 Worth St., New York 13, N. Y.

A revision of its popular 1950 Wall Chart, Eutectic's latest edition is in the form of a handy 6-page folder which can be filed or bound or unfolded to an 11- x 23-in. chart for wall mounting.

Detailed information is given for each alloy and electrode, covering: its composition; heating facilities which can be used; metals on which to use; type and preparation of joint; preheating of parent metal; color match rating with metals for which listed; approximate heat and corrosion ratings, etc.

Included also, is a full-page listing of "1001 Ways to Save in Your Plant," covering a wide variety of everyday welding applications and the alloys recommended for maximum efficiency in each instance.

For free copies of this handy folder, write directly to Eutectic Welding Alloy Corp., 40 Worth St., New York 13, N. Y., requesting Chart TIS 246 P. Please be sure to mention the name of this publication when writing.

1950 Book of A.S.T.M. Methods for Chemical Analysis of Metals

The new *1950 Book of A.S.T.M. Methods for Chemical Analysis of Metals* is a greatly expanded volume, replacing the 1946 edition. It is the culmination of intensive work through many months by A.S.T.M. Committee E-3 on Chemical Analysis of Metals and the Headquarters Staff. The membership of Committee E-3 (over 100) includes many of the leading technologists in this field. The new edition provides in their latest re-edited form all of the previous methods and it is supplemented by many new testing procedures which the committee has studied and improved, and which have subsequently been approved by the Society.

Now containing 39 extensive standards, covered in 486 pages, the publication is the only one where these chemical methods appear together in a convenient book form. The newer methods included take advantage of shorter procedures which have been developed and also cover additional metals. It is also worth noting that the several spectrochemical methods included were prepared by Committee E-2 on Emission Spectroscopy.

Welding with Multiple Electrodes in Series

The series-arc technique of submerged-melt welding, a production welding method in which the depth of penetration of the weld metal into the base metal can be controlled, is explained in a new, illustrated booklet, "Welding with Multiple Electrodes in Series—A New Method of Unionmelt Welding." Use of this method in cladding operations with stainless steels, surface applications with some hard-facing rods and in nonferrous cladding and surfacing applications are discussed.

The booklet may be obtained without charge from the nearest office of The Linde Air Products Co., or by writing direct to The Linde Air Products Co., 30 E. 42nd St., New York 17, N. Y.

Buyers Guide

A new 32-page booklet, just issued by All-State Welding Alloys Co., Inc., 273 Ferris Ave., White Plains, N. Y., under the title, "Buyers Guide to the Complete Line of All-State Alloys and Fluxes for Welding, Brazing, Soldering, Cutting, Tinning," tells everything a buyer needs to know to select the particular alloys and fluxes that best meet his needs. The material is organized and cross referenced so that right selection is virtually automatic. The formula, as outlined in the booklet, is simply, "Having a certain kind of metal to work on, you find it in the first column and that points out the All-State products recommended to you. In the next column your choice is narrowed by whether you are going to use torch, arc or soldering iron. From this point on, guided by working temperatures, strength and comments related to your specific problem, you can readily select the rod that meets your requirements most exactly."

Copies are available on request and without obligation.

National
Carbide
IN THE RED DRUM
EFFICIENT
ECONOMICAL
DEPENDABLE

FOR WELDING and CUTTING
Use National Carbide in the Red Drum
Write us for information as to nearest available stock.
60 E. 42nd St. NATIONAL CARBIDE COMPANY New York 17, N.Y.
A Division of Air Reduction Co., Inc.

1 Hammer Set . . .

with the service life of 7!

This new Selector and Comparison Chart . . .

Describes the rods in the complete AMSCO Line

Lists the type of service recommended for each rod

Compares other makes for handy cross-reference.

Here are facts that help increase efficiency and economy in your hardfacing work. Write today for your free copy of Bulletin CC-3.

Hardfacing with AMSCO® TUNGSTEN CARBIDE made these Pulverizer Hammers last 600% longer!

Here's another case where the use of AMSCO Hardfacing Rod has resulted in saving hundreds of dollars—plus shorter and less frequent down time for repairs. The hammers above, used for pulverizing asphalt roofing trimmings, were subject to severe abrasion . . . average service was only 4 days. To stop such rapid and costly wear, the hammer tips were hardfaced with AMSCO Tungsten Carbide. Result? Instead of only 4 days, *the same hammers now last 4 weeks!*

Equally big savings are being made every day with AMSCO Tungsten Carbide—on such parts as:

Drill Bits	Cane Knives	Pug Mill Knives
Plowshares	Muller Plows	Hammermill Hammers

Wherever parts are subject to abrasion or impact . . . or wherever serrated cutting edges are used . . . you'll find money-saving, time-saving applications for AMSCO Hardfacing Rods and Electrodes.

put LIFE into service with

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WELDING PRODUCTS

AMERICAN Brake Shoe COMPANY

AMERICAN MANGANESE STEEL DIVISION
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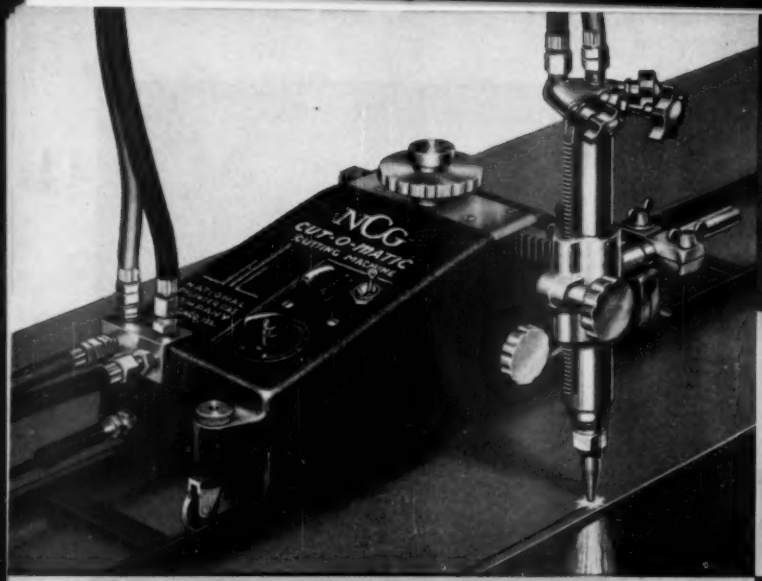
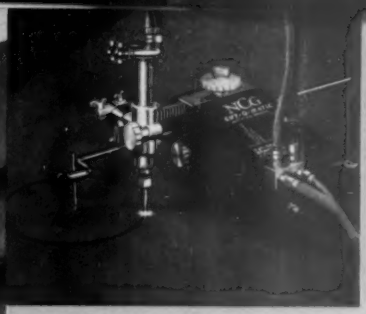


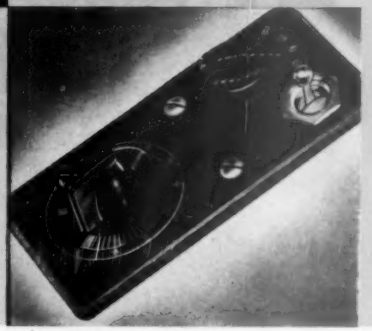


CIRCLES AND ARCS

Circles or arcs, up to 60-in. in diameter, are cut by attaching radius rod assembly. Larger diameters may be cut with extension rod. Rings may be cut with double torch mounting.

SIMPLE CONTROLS

The Cut-O-Matic Control Panel is simple. A selector switch sets the travel at one of four speeds, and a rheostat permits accurate regulation anywhere between 4-in. and 60-in. per minute, in either direction.



CUT-O-MATIC

NCG'S PORTABLE, MOTOR-DRIVEN FLAME-CUTTING MACHINE

LOW-COST, VERSATILE, ACCURATE, FAST

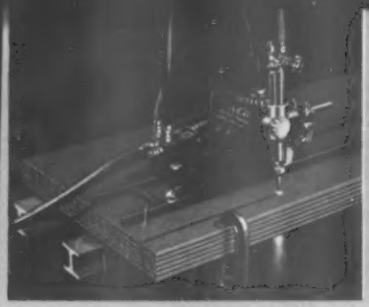
Flame-Cutting with Substantial Savings in Labor and Materials

Here's a small, inexpensive cutting machine that's so efficient that grinding or machining of cuts are rarely necessary . . . That's so versatile you can cut light plate or heavy slabs—straight lines, bevels or various shapes . . . That's so portable you can take the machine to the job—inside or outside the shop, wherever power is available. And it has exclusive operating features that make it remarkably easy to handle.

Yes, the NCG Cut-O-Matic is so efficient and so flexible in use that one man and a Cut-O-Matic make a hard-to-beat team to save time, labor, and materials.

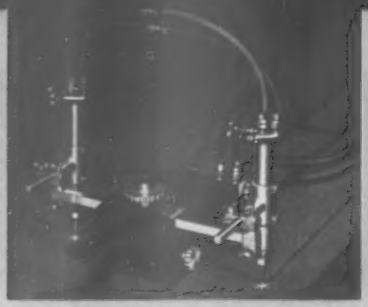
The Cut-O-Matic is not only a money saver in the larger plant but in small shops, too. In countless instances the low price of the Cut-O-Matic has enabled small businesses to enjoy the recognized superiority of machine cutting—with its sharper, cleaner cuts and its overall economies in time, materials and gas.

Cut-O-Matic offers so many advantages that you'll want a copy of the illustrated booklet describing the unit in detail. If you'd like a demonstration right in your own shop, just check the box in the coupon at the right.



STACK CUTTING

Stack cutting gives real production economy and speed. Sheets clamped together may be cut uniformly. Circles and arcs as well as straight lines are easily stack-cut.



STRIP CUTTING

Strips up to 29-in. wide can be cut by adding torch holder, torch and hose assembly. Wider strips may be cut by using extra length equipment. Narrow strips may be cut by mounting two torches side by side.

IRREGULAR SHAPES

Limited irregular shapes may be cut by guiding the Cut-O-Matic by hand. Rear wheel is castered to facilitate travel. Cuts are much better than those made with a hand cutting torch.

FREE-WHEELING

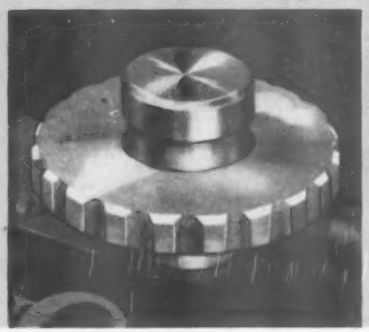
Fingertip clutch control is combined with a cross-feed gear rack adjustment to permit moving the machine on track while adjusting the cross-feed, in one simultaneous operation.

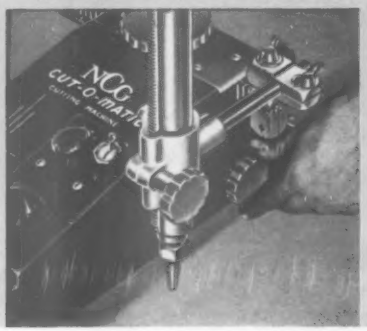
EASY CHANGE-OVER

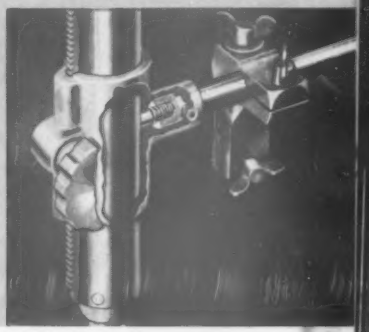
It's easy to switch from cutting straight-line to circles. The friction knob is loosened, as illustrated, and the inside drive wheel is disengaged. The outside wheel then propels the machine around the center point.

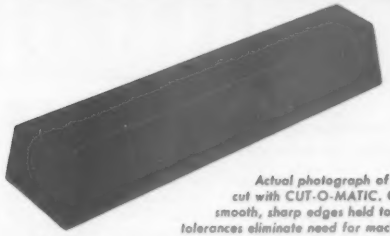
SMOOTH—RIGID

No ragged cuts caused by excessive play and uneven racking. A special clamp adjustment, plus a spring-loaded pressure plug, holds torch in rigidly, yet permits smooth racking.









Actual photograph of bevel cut with CUT-O-MATIC. Clean, smooth, sharp edges held to close tolerances eliminate need for machining or grinding.

NCG CUT-O-MATIC STANDARD UNIT

F.O.B. FACTORY, LOUISVILLE, KY

$210

Complete with torch, tip, radius rod, track, lens, cable and hose wrench.

CUT-O-MATIC possesses all the quality characteristics of famous NCG heavy-duty flame cutting equipment. Sturdy, simply designed for efficient operation and easy maintenance, the CUT-O-MATIC is a dependable tool that will give you long trouble-free service.

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Please send me Illustrated Booklet N-134 describing in detail the versatile CUT-O-MATIC Flame-Cutting Machine.

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☐ I would like a demonstration in my own shop.

EutecRod 14FC

In order to supply up-to-date information on the uses and applications of EutecRod 14FC, the popular welding rod for high-strength welds on cast iron by means of oxyacetylene torch, Eutectic Welding Alloys Corp. offers a new data sheet which has just come off the press. Illustrated with several case histories showing typical jobs performed with this versatile alloy, this data sheet also contains a listing of physical properties of the alloy.

EutecRod 14FC has found wide acceptance throughout American industry, wherever cast iron must be welded. Despite the fact that the alloy is applied up to 1000° F. below the melting point of the base material without puddling or fusion, the completed weld achieves a tensile strength of up to 50,000 psi., which is generally more than that of cast iron itself. In addition, this fully machinable alloy offers a perfect color match with the base metal, making it possible to obtain neat, "impossible-to-detect" joints or repairs.

The data sheet for this alloy, EutecRod 14FC, is available upon request from the Technical Information Service, Eutectic Welding Alloys Corp., 40 Worth St., New York 13, N. Y.

Industrial Research Laboratories

The ninth edition of the directory *Industrial Research Laboratories of the United States* (445 pp., $5.00) is now available from the National Research Council. The book contains information on the research laboratories of 2845 industrial and commercial organizations, including the names of the president and research executives; the location of the laboratories; the numbers and kinds of research scientists and other personnel of the research laboratories; the fields of research activity; whether any consulting services are offered and the size of the laboratory library.

The directory contains two appendixes, one giving the same information for government laboratories which cooperate with industry, and the other listings for universities and colleges which offer research services to industry, including the kinds of facilities available. The book also contains a geographical index and a complete subject index of research activities.

The new edition may be ordered from the Publications Office, National Research Council, 2101 Constitution Ave., Washington 25, D. C. Checks should be made payable to the National Academy of Sciences.

Ampco News

Copies of the fourth quarter issue of the *Ampco Welding News* are now available from Ampco Metal, Inc. This issue includes articles on the use of welded overlays on a newly designed pipe bending machine, the fabrication of heavy-duty screw conveyors 19½ ft. long by 12 in. in diameter, the welding of huge galvanized iron air supply ducts and other articles of equal interest.

Hard Surfacing

"Hard Surfacing Manganese Steel," is the title of a 4-page bulletin just released by Rankin Mfg. Co., Los Angeles, manufacturers of Ranite hard-surfacing welding materials. The article outlines applications, precautions and suggestions on the proper procedures. Basically it states that the success in the facing of manganese is in avoiding reaching high temperatures throughout the body of the steel, a precaution which should be observed on pieces even as small as a shovel tooth. It is stated that where precautions as outlined in this article have been observed, manganese has been successfully hard surfaced, saving users of manganese steel many thousands of dollars.

Copies of "Hard Surfacing Manganese Steel" may be had by writing Rankin Mfg. Co., 3072 W. Pico Blvd., Los Angeles 6, Calif.

PERSONNEL

Newcomen Society Awards Medal for Advances in Steam

The Newcomen Medal "for achievement in the field of steam" was awarded recently to Isaac Harter, chairman of the Board, The Babcock & Wilcox Tube Co., Beaver Falls, Pa., at a joint meeting of the Newcomen Society and The Franklin Institute in honor of Benjamin Franklin's birthday. This was the second time the medal had been conferred, the first being to Rear Admiral Harold C. Bowen, U.S.N., in 1944.

Among Mr. Harter's achievements in steam which led to his nomination for the Newcomen Medal were the furthering of research to remove diffused oxygen from boiler feed water and the application of this research to boilers into which were constructed economizers built with steel tubes. This development removed the cause of corrosion which had been preventing use of steel economizer tubes. Formerly the tubes were of cast iron which could withstand only 200-lb. pressure.

Mr. Harter also assisted in the discovery of the effect of caustic feed water on steel, and helped find ways to prevent such caustic action. He developed steam drums made with butt-strap joints caulked on the inside, thus preventing any harmful action, and eventually pioneered welded joints in boiler drums which accomplished this result and at the same time made it possible to build boilers to operate at pressures far beyond the limitation of those imposed by riveted joints. Through his efforts boilers having welded joints were first accepted by the U. S. Navy, an important factor in their later adoption by the A.S.M.E. code. He also developed the method of applying X-ray to the testing of welded boiler drums and made a successful stand to have the A.S.M.E. code stipulate that such joints should be X-rayed.

The medal bears the inscription: "The Newcomen Medal Founded by the Newcomen Society of England." In the center is inscribed: "Awarded by The Franklin Institute of the State of Pennsylvania, to Isaac Harter for achievement in Steam." The reverse side displays the seal of the society.

OBITUARY

Harry J. Koerner

Harry J. Koerner of Kenmore, N. Y., sales engineer for the Air Reduction Co., died recently at his home.

Mr. Koerner was a member of the AMERICAN WELDING SOCIETY since 1937. He was born in Long Island City and came to Buffalo in 1919 after serving overseas in the Army during World War I. He had been with the Air Reduction Co. 31 yr. and was considered an authority on welding processes.

Mr. Koerner was a member of a number of clubs and associations, including the Rochester Chamber of Commerce.

Surviving are his wife Lorna Taylor Koerner, and three sons, William J., Robert T. and Harry J. Koerner, Jr.

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JIM: WHAT ELECTRODE WILL GIVE US--
1. HIGH QUALITY DEPOSITS-PERFECT X-RAY. NO UNDERBEAD CRACKING
2. WELDS IN HIGH QUALITY STEEL WITHOUT POROSITY OR CRACKING
3. LITTLE OR NO PREHEATING
4. HIGH DEPOSITION RATES
5. LOWER COST THAN STAINLESS ROD NOW USED
— BILL
Bill: Your best bet —
new P&H Low Hydrogen Electrodes!
Jim

Now at low cost!

P&H LOW HYDROGEN ELECTRODES

For welds with superior physicals that match special steels, simplify production — —

Here's the world's most complete line of low hydrogen electrodes — 14 types to choose from.

Operators prefer them because of their easy manipulation, easy slag removal, smoother deposit, low spatter loss, fast deposit rate, good penetration, and less smoke.

Manufacturers prefer them too. They have fewer rejections. Smaller welds for faster production. Fewer types of rods to buy, stock, and supervise. They eliminate trouble with off-analysis steel.

Simplify your welding procedures. Standardize on P&H Low Hydrogen Electrodes. Your costs are lower, your welds better and stronger.

P&H
WELDING DIVISION
4551 W. National Ave.
Milwaukee 14, Wisconsin
HARNISCHFEGER CORPORATION

2255

Here are a few of the money-saving P&H Low Hydrogen Electrodes:

- *For problem steels:* High carbon, high sulphur, cold-rolled alloy . . . 70LA and 70LA-2.
- *For steel castings repair;* matches analysis and heat-treating properties of Grade B castings . . . P&H .25C.
- *For steel castings repair;* matches heat treating properties of Grade B castings . . . 70LB.
- *For welding nickel-alloy steels;* gives high impact resistance at low temperatures . . . 75LP.
- *For Chrome-moly steels* (1% Cr., ½% Mo.) used in power piping . . . 80LE.
- *For Chrome-moly steels* (2¼ Cr., 1% Mo.) used in power piping . . . 90LE.
- *To match heat-treating properties of SAE 1040* and like steels. Also for steel-castings repair . . . P&H .40C.
- *For welding high hardenable steels.* Also steel castings repair . . . AW2B.
- *For repair welding Grade C castings* and steels of similar composition . . . P&H-7.
- *Comparable to SAE 8630 steel* . . . excellent heat-treating and flame-hardening characteristics . . . 90LH-2.
- *For welding high hardenable steels* without pre-heat such as re-rolled rail stock . . . P&H-12 and P&H-17.
- *For aircraft and similar steels* . . . has wide range of properties under heat treatments . . . P&H-21.

Excavators • Overhead Cranes • Hoists • Arc Welders and Electrodes • Soil Stabilizers • Crawler and Truck Cranes • Diesel Engines
Cane Loaders • Pre-assembled Homes

NEW PRODUCTS

Argon Regulator-Flowmeter

A new, direct-reading combination regulator and flowmeter for the measurement of argon flow is announced by The Linde Air Products Co., a unit of Union Carbide and Carbon Corp. The regulator-flowmeter, R-502, is designed for use with heliare welding, argon metal-arc welding and other related processes.

The regulator-flowmeter has a new 3-stage type regulator that maintains constant inlet pressure and accurate flow. Factory presetting of the second stage of the regulator delivers argon at a constant pressure of 20 lb. per square inch. The flowmeter, with a finger-tip control adjusting valve, permits accurate, steady flow rates up to 60 cu. ft. per hour.

The rate of flow is indicated on a float-type, easy-to-read flow tube that is calibrated in cu. ft. per hour, the unit of measurement generally used in inert-gas-shielded-arc welding. The reading is accurate because the flowmeter is not affected by back pressure from the equipment side.

The same design features are incorporated in a second regulator-flowmeter, the R-503, which is available for use with helium. The flowmeters of both regulator-flowmeters are also available separately. The L-23, the argon flowmeter, can be used with a standard oxygen regulator such as the R-64. The L-28, the helium flowmeter, may be used with the R-67 hydrogen regulator. The L-28 is calibrated for helium flow in cu. ft. per hour, and it will measure flows up to 175 cu. ft. per hour.

New Double Sleeve Coat

Metal & Thermit Corp., 100 E. 42nd St., New York 17, announces the addition of a new model welders coat to its line of welders protective clothing. The garment is a 26-in. length coat made of soft, pliable chrome tanned split cowhide in small, medium, large and extra large sizes. It is generously cut for greatest comfort

and sleeve fronts are of a double thickness of leather to assure long wear. Other features include double stitching, snap fasteners, adjustable wrist snaps, seam end reinforcement with copper rivets. Literature is available.

Mash Welder

"Mash" welding is a relatively new process of resistance-welding technique employing the use of either a seam or projection type welder to obtain a clean smooth end-to-end joint without metal overlap or protruding metal on the joined edges of the sheets. This technique resulted from considerable research and experience to eliminate the "flash" made by conventional resistance-butt welding methods—which has the disadvantage of requiring a grinding or machining operation to remove the upset flash material.

In conventional seam welding the sheets are ordinarily overlapped $^3/_8$ to 1 in. to provide a sufficient overlap of metal without sheet deformation and "spitting" of the weld metal at the faying surfaces. In "mash" welding, however, the amount of overlap is limited to approximately $1^1/_2$ times the sheet thickness with welding pressure correspondingly increased to almost twice that required for conventional seam welding—thus forcing the plastic metal without any irregularities or metal ejection from an overlap of 2 sheets into a single sheet only slightly thickened at the weld joint. The joint in this condition is suitable for many purposes without further grinding or other finishing.

The Sciaky PMM.2TL Press Type Mash Welding Machine has been designed and built for a specific application of joining a 28 in. width of mild steel sheet to form a continuous line for various mill operations.

This machine incorporates the Sciaky Patented Three-Phase System, and is equipped with a locating and clamping table to automatically locate the sheets to a proper overlap and hold them securely for the welding operation. Immediately following the welding, two steel idling wheels perform a rolling operation which reduces the overlap of the two sheets to a minimum thickness. This thickness can also be controlled by varying the amount of pressure provided by the upper-head assembly. In actual operation the operator need only to insert the ends of the sheet to be welded to a stop between the electromagnetic clamping bars and depress a control button which clamps the sheets securely and rocks the clamped piece parts with a preadjusted overlap to the welding position.

Literature giving full specifications on the welder is available upon request to the manufacturer: Sciaky Bros. Inc., 4915 W. 67th St., Chicago 38, Ill.

Argon Metal-Arc Hand-Welding Equipment

A new hand torch and automatic wire-drive unit for argon metal-arc welding has been announced by The Linde Air Products Co. Now, faster welding can be performed than has ever been possible with hand-welding equipment. The consumable electrode serves as the filler metal. Welding rod is fed from a coil into an argon-protected atmosphere at a steady predetermined rate.

The unit consists of the Linde FSH-4 argon metal-arc hand-welding torch and the FSM-2 rod feed unit. It is particularly adaptable for welding aluminum in ranges of thickness from $^1/_8$ to $1^1/_2$ in. Hand welding can be applied on butt, lap, fillet, edge and corner joints in the overhead and vertical, as well as in the horizontal and flat positions.

Some of the features of this new welding unit are:

1. Automatic operation. Rod feed, argon flow and cooling water are automatically controlled.

2. Electronic governor for accurate and uniform control of rate of rod feed. Rod feed rate varies from 80 to 380 in. per minute.

3. Lightweight construction. Unit is readily portable, yet durability has not been sacrificed.

4. Flexibility of hose and cable entering the torch handle.

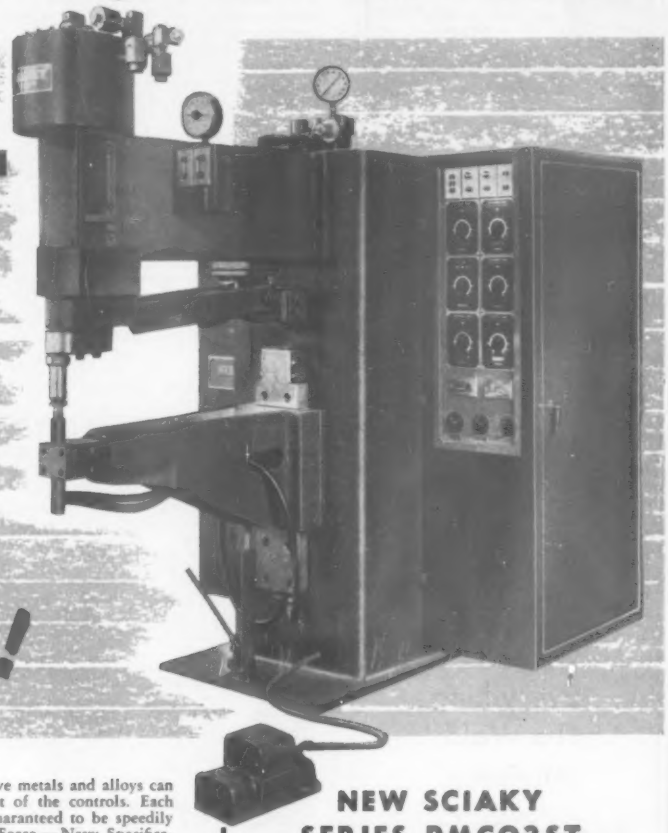
5. Convenient design of pistol-shaped grip of torch reduces operator fatigue and

For Aircraft Welding to ANW Specifications

aluminum
magnesium
brass · mild steel
monel · inconel
stainless steel
other alloys
handled

with

THE SAME WELDER!



• WITH this one machine all of the above metals and alloys can be easily welded by a simple adjustment of the controls. Each Sciaky Modu-Wave Aircraft Welder is guaranteed to be speedily qualified to ANW-30 and ANW-32 Air Force — Navy Specifications right in your own plant at no extra charge to you at time of installation and tryout.

Quality of welds is unsurpassed with tip life increased as much as 300% over previous welders.

These machines handle a wider range of work, will weld faster and more consistently and because of their greater productivity can be more rapidly amortized.

Today, Sciaky Aircraft type welders represent 80% of the machines being used for welding to ANW Specifications. This achievement was brought about by constant research and development in this specialized field. Write today for full particulars on how this versatile equipment can improve your welding and reduce production costs.

SCIAKY BROS., INC.
4921 W. 67th St.
Chicago 38, Illinois

SCIAKY
SAY "SEE-ACKY"

NEW SCIAKY SERIES PMCO2ST Three-Phase Modu-Wave Welder

100 KVA at 50% duty cycle
220 and 440 volt 3-phase, 60 cycle*
Standard Throat depth 36"

Welding Range to AN-W specifications (2 thicknesses): aluminum and magnesium alloys .025" to .081"; stainless steel .022" to .109"; low carbon steel .022" to .156".

* Available with special controls for 25 and 50 cycle.

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Complete details, description of controls, features, specifications are included in Bulletin 134-ST. SEND FOR YOUR COPY TODAY.

"Every one of our Maintenance Weldors Should Carry An

ALL-STATE DOC ALLOYS KIT.

It makes Him Ready For Any Job. Gives Him Everything To Weld Anything.

Enough To Save Many Times Its Cost."

Ask your ALL-STATE Distributor for ALL-STATE DOC ALLOYS KIT

Price **$15**

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ALL-STATE

WELDING ALLOYS CO., INC.

273 Ferris Avenue
White Plains, New York

provides better control over welding operation.

6. Facility in setting up makes more arc time available for greater production.

7. Simplicity of operation. Once the unit is assembled and the controls preset, operation is very simple.

Argon metal-arc welding has been performed on metal thicknesses from 1/16 to 3/4 in. in aluminum and copper alloys, stainless steel and similar metals. For heavy plate, the multipass technique makes possible the welding of any thickness. Argon metal-arc welding is well suited to welding of railroad tank cars, large storage tanks, pressure vessels, synthetic gasoline equipment, brewing equipment, and surface buildup operations.

For further information about equipment for argon metal-arc welding, readers are invited to write to the nearest office of The Linde Air Products Co., or to the General Office at 30 E. 42nd St., New York 17, N. Y.

A.-C. Arc Welders

Marketing of a new series of industrial a.-c. arc welders has been announced by Marquette Mfg. Co. of Minneapolis, for over 30 years a leader in the a.-c. welding field.

The new Marquette industrial welders are available in three sizes—200, 300 and 400 amp. capacities—and are designed for heavy-duty, 'round-the-clock production welding.

One of the key construction features of the new 80 series welders is the use of Hipersil steel transformer cores, which provides 1/3 greater flux-carrying capacity, reducing power consumption and operating costs.

Marquette Mfg. Co. manufactures a complete line of arc-welding equipment and accessories, arc-welding electrodes, oxyacetylene welding and cutting equipment, gas welding rods, battery chargers and towing equipment.

Buyers' Guide to British Industries Fair

American buyers visiting the British Industries Fair this spring can take advantage of many special arrangements made for the convenience of overseas businessmen. This great annual trade exhibition, scheduled for London and Birmingham (England) from April 30th through May 11th will put British goods on display in record volume and variety.

From the British Consulates in 24 major U. S. cities, buyers can obtain advance copies of the B.I.F. catalog, and receive introductions to trade and business groups in Britain.

Flexible Automatic Welding

When the Great Northern rebuilt 400 of their ore cars, the Unionmelt flexible welder was used for many operations. Welds were made at high speeds in places hard to get at with other automatic welding machines—the flexible hose and nozzle covered an area of 1000 sq. ft. But even this range was increased for one of the operations on the new car baskets.

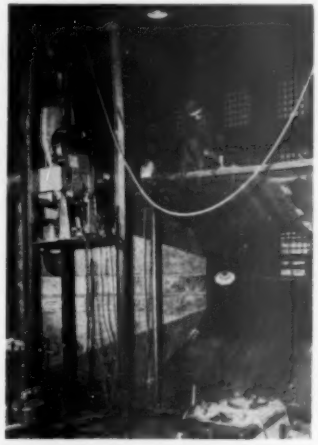

Fig. 1 By using an elevator device the range of this flexible welder was increased 80 per cent

Fig. 2 Here's the flexible welder in action

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THE greater arc stability of welding rod coatings having *higher* and *more uniform* titanium dioxide content is readily achieved with new, non-pigmentary TITANOX-TG. Unlike pigmentary grades of titanium dioxide, this new product is remarkably free from phosphorus, sulfur and volatile metals including zinc and antimony. In comparison with pigmentary titanium dioxide, TITANOX-TG requires much less silicate binder. Thus, not only can the TiO_2 content of the coating be appreciably increased but also accurately controlled through high purity.

This highly purified titanium dioxide is free-flowing, non-sticking and non-balling. It will contribute to faster and more economical production, particularly when dry blending is employed. We shall be glad to help you adapt TITANOX-TG to your titanium coated electrodes. Write today to our Technical Service Department. Titanium Pigment Corporation, 111 Broadway, New York 6, N. Y.; Boston 6; Chicago 3; Cleveland 15; Los Angeles 22; Philadelphia 3; Pittsburgh 12; Portland 9, Ore.; San Francisco 7. In Canada: Canadian Titanium Pigments, Ltd., Montreal 2; Toronto 1.

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Subsidiary of NATIONAL LEAD COMPANY

SECTION ACTIVITIES

prepared by C. M. O'Leary

Anthony Wayne

Round and Square Dancing with John Wright, Caller was enjoyed by the members and guests of the Anthony Wayne Section on Dec. 15, 1950 at Lunz Barn, Elmhurst Gardens. Eats and drinks were served by the Brudi's Catering Service.

Arizona

The December meeting of the Arizona Section was held in the Saratoga Room of the Hotel Westward Ho at 8:00 P.M. on Wednesday, December 20th. Approximately 30 members were present.

A very interesting talk was given by Mr. Mussman, Western Area Manager of the Eutectic Welding Alloys Corp. He spoke on "New Applications of Low Melting Nonfusion Welding Filler Metals." The talk was well received and prompted an interesting discussion.

Gus Waago, Manager of Consolidated Welding Supply Company of Phoenix also gave an interesting talk on "Stud Welding" and showed the new stud-welding technicolor sound film "Split Second Fastening."

To conclude the program, the United States Steel film on the Erection of the Secretariat Building of the United Nations was shown through the courtesy of the local office of the Consolidated Western Steel Corp.

The meeting was adjourned at 10:45 P.M.

Boston

The regular monthly dinner meeting of the Boston Section for December was held on the 11th at Graduate House, M.I.T., Cambridge. Dinner was held in the Campus Room. As a coffee feature a color movie on Yellowstone Park was shown and very well received.

Technical speaker was J. W. Mortimer of the Whitlock Mfg. Co., Hartford, Conn. His subject was "Welding Heat Exchangers for the Chemical Industry." Mr. Mortimer was the first Chairman of the Hartford Section. He is now in charge of all welding applications and developments at the Whitlock Mfg. Co. He gave a very able and interesting talk on inert-arc welding.

Chicago

The regular monthly dinner meeting of the Chicago Section for December was held on the 15th at the Peoples Gas Light & Coke Co. auditorium. Dinner preceded the meeting at Burke's Grill.

A premeeting movie was shown of a holiday outing via Pan American Airlines around South America.

Technical speaker was J. R. Wirt of Delco-Remy Div., General Motors Corp., Anderson, Ind. His subject "Development of Production Welding Techniques" was well presented and the slides were excellent. So was the subject matter as Mr. Wirt gave facts on dollar savings with each slide.

Cleveland

T. B. (Ted) Jefferson, editor of *The Welding Engineer* addressed the Cleveland Section at its third regular meeting of the season on December 13th at the Allerton Hotel. Mr. Jefferson's paper on "Maintenance Welding" was well illustrated with many slides. He compared the relative effect should there be a sudden elimination of maintenance welding to the big recent strikes on our economy. By comparison, the strikes resulted in a relative small slow down of the economy; no maintenance welding would result in a complete and total stoppage of our economy in 40 days.

Some of the many illustrations described in detail were the operations of three welding crews on the Queen Mary and Queen Elizabeth repairing rivet leaks to keep the ships afloat, the welding of an engine during World War II in a ship built in 1909, the welding of Missouri river dredge pumps handling 40,000 g.p.m. of abrasive material, and the welding of wicket gate shafts at Hoover Dam.

Mr. Jefferson was introduced by Lew Gilbert, editor of *Industry and Welding*. The main program was preceded by a documentary film on the Bethlehem Steel Corp.'s Venezuelan iron ore development by the McDowell Co. McDowell engineers made comments during the showing of the film.

The Cleveland Section will send a corps of experts to Detroit on February 9th, to compete at the Detroit Section's Quiz the Experts meeting.

The Cleveland Section's eight weeks educational course is under way at Case Institute of Technology. Professor Jerry Cover is the teacher and the course deals with the metallurgy of welding.

Norman Carl, a valued member of the Cleveland Section, was taken to Polyclinic Hospital, December 15th, where he is recuperating from a heart attack. He will require several months rest before returning to his job at the Burdett Oxygen Co. The Cleveland Section is grateful that he has been spared and extends sincere wishes for a complete recovery.

Colorado

The December 13th meeting of the Colorado Section was held at the Oxford Hotel, Denver, in the form of a "Stump the Experts Program." This was the Section's initial trial of a program of this sort. H. Jackson, Supt. of Thompson Pipe & Steel acted as M.C. He was assisted by D. C. Card of C.S. Card Iron Works; H. B. Klodt of the Silver Engineering Works; H. K. Schmuck of The Linde Air Products Co., and Herman Geller of Stearns Roger Mfg. Co. This was a most interesting meeting, well handled, and it is difficult to understand how men interested in the art of welding will pass up such a meeting.

Columbus

December 8th dinner meeting had an attendance of 45 at dinner and meeting. Technical speaker was E. B. Lutes, Director of Production, Arcrods Corp., Sparrows Point, Md. His subject "Arc-Welding Electrodes" was presented in a clear cut, interesting manner.

Dallas

The December meeting of the Dallas Section was held on the 11th in the Auditorium of the Lone Star Gas Co. Dinner was held in the Brockle Restaurant.

An illustrated talk was presented by J. E. Dato of The Linde Air Products Co., on "Argon Metal-Arc Welding of Ferrous and Nonferrous Alloys."

Refreshments were served after the conclusion of the meeting.

Dayton

The December meeting of the Dayton Section was held on December 12th at the Engineers Club in Dayton. The technical speaker was Jack Ogden of the Fisher Body Division of General Motors Corp., Detroit. The subject of his talk was "Welding in the Automobile Industry."

The speaker covered many of the applications of resistance, arc, inert-arc and direct-arc welding in the manufacture of automobile bodies as well as some applications of gas welding and brazing. Many slides were shown illustrating the type of welding presses used for resistance welding and the method in which material is fed into the presses and held during welding. Other slides were shown to illustrate the types of transformers used with various resistance welding electrode arrangements.

Prior to the technical session a movie

WHAT'S THE RIGHT X-RAY FILM?

Product:	Material:	Equipment:
Welded pressure vessel	2-1/2" cold rolled steel	250 kv x-ray machine

ANSWER:

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To examine this pressure vessel, the radiographer was faced with a thick section of dense material, and with x-ray equipment low in power for the size of the job.

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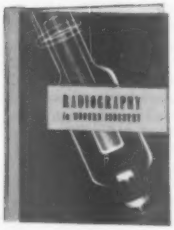
Type F, exposed direct or with lead-foil screens, gives good results where wide exposure latitude is required, such as when a wide range of section thicknesses is to be recorded in a single radiograph.



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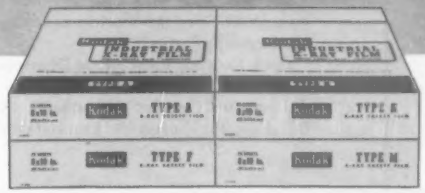

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To provide the recording medium best suited to any combination of radiographic factors, Kodak produces four types of industrial x-ray film. These provide the means to check welds efficiently and thus extend the use of the welding process.

Type A—has high contrast with time-saving speed for study of light alloys at low voltage and for examining heavy parts at 1000kv. Used direct or with lead-foil screens.

Type M—provides maximum radiographic sensitivity, under direct exposure or with lead-foil screens. It has extra-fine grain and, though speed is less than in Type A, it is adequate for light alloys at average kilovoltage and for much million-volt work.

Type F—provides the highest available speed and contrast when exposed with calcium tungstate intensifying screens. Has wide latitude with either x-rays or gamma rays, exposed directly or with lead screens.

Type K—has medium contrast with high speed. Designed for gamma ray and x-ray work where highest possible speed is needed at available kilovoltage without use of calcium tungstate screens.

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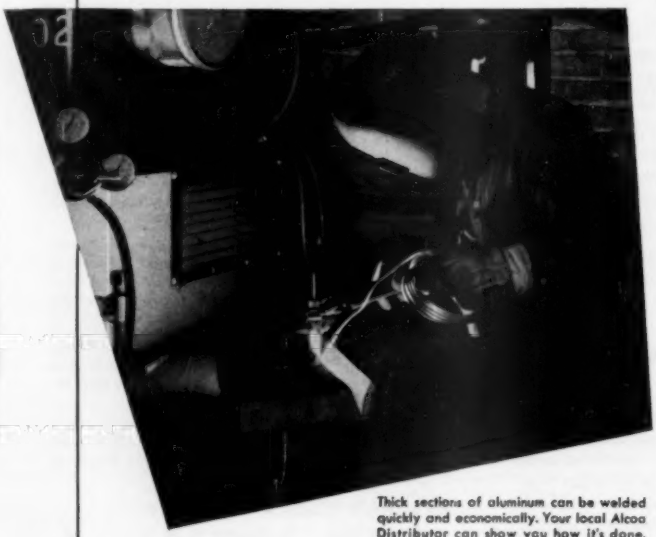

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ALCOA FIRST IN ALUMINUM

was shown depicting modern life in the Great Smoky Mountains after the advent of electricity.

The January meeting of the Dayton Section was held in the Engineers Club on Tuesday, Jan. 9, 1951. The meeting started off with a color movie on Yosemite Valley in California.

The technical speaker was L. E. Kunkler of the Metallizing Company of America. Mr. Kunkler is the father of this industry and has been connected with it continuously since its inception. He showed slides of various methods of applying the metallizing process and a great many applications of it. Mr. Kunkler is President of his company and President of the International Metallizing Assn.

Detroit

It was most unfortunate and disappointing to the Detroit Section that only 55 of their membership turned out to hear Ronald Haughton's talk on "The Administration of Collectively Bargained Pension Plans," since his wealth of information on collective bargaining and pension plans provided a most interesting and timely subject as evidenced by the interest displayed by those in attendance in the lengthy discussion period which followed his talk. The information and data provided by Mr. Haughton was of general interest since for those connected with management it provided a basis for comparison, and for those interested from an employee standpoint it "pointed up" the ever growing trend toward Old Age Security.

The most significant point of all probably was the effect that pension plans will have on "job jumpers." It would appear that younger workers would give little consideration toward pension plans in choosing between jobs, however, as men reach middle age, pension plans would receive serious consideration when anticipating changing employers.

Mr. Haughton is contract umpire and impartial chairman of the Ford Motor Co.—U.A.W. (C.I.O.) Joint Administration Board.

Provincial Parks Department film "Fishing in Canada" was also shown.

The above meeting was held on Dec. 8, 1950.

Houston

Dinner at the Sheffied Steel Co. Dining Room preceded the plant inspection of A. O. Smith Corp., Houston Plant on Friday, December 15th. One hundred members and guests were present. The tour included the inspection of pressure vessel fabrication of all types; high production manufacture of 115–1000 gallon Liquified Petroleum Gas tanks, and manufacture of deep well turbine pumps.

Kansas City

The annual Christmas Party for members, wives and guests, was held on December 9th at the Fairfax Airport, Kansas City, Kans., with an attendance of 44.

Louisville

The November meeting of the Louisville Section was held on the 28th in Kapfhammer's Restaurant, and featured a Quiz Session. Following a short business meeting, a series of practical questions on welding problems were discussed from the floor and timely comments added by some participants who were particularly qualified in certain fields. The meeting closed with a Dutch lunch and a social hour.

Maryland

The second meeting of the season was held on November 17th at the Engineers Club of Baltimore.

The after-dinner speaker was Rudolph J. Matousek, the Y.M.C.A. Physical Educational Director, who spoke on physical fitness. Mr. Matousek tried to make the audience more acutely aware of the attention that is paid to such inanimate objects as their automobiles and comparing that to the lack of attention paid to their physical condition.

The main speaker of the evening was Robert A. Wilson, Application Engineer, Lincoln Electric Co. The subject of Mr. Wilson's talk was "Possibilities of Automatic and Semiautomatic Welding." The semiautomatic machine uses a $^{1}/_{32}$-in. wire, or smaller, with a flux which places the method in the submerged-arc category. The flux fuses and shields the deposited metal in the same manner as the slag in the open-hearth method of making steel.

Numerous methods of using the semiautomatic system were discussed such as attaching a small shaft to a large shaft by means of a fillet weld and thereby saving machining hours and subsequent costs. One end of the small shaft was ground to a conical shape and two welding passes provided 100% penetration. The secret of such attaching lies in the drilling of a small vent hole through both the cylindrical and conical portion of the small shaft and thereby preventing porosity in the weld deposit.

Michiana

The Michiana Section joined forces with the Notre Dame Section of the American Society for Metals for a session on "Atomic Power—Tool or Weapon." The speaker for the evening was Marvin V. Maxwell, Asst. Northwestern Engineering and Service Manager, Westinghouse Electric Corp. Some 50 members and guests braved the snow and cold to hear the talk.

Mr. Maxwell touched briefly on the basic concepts of nuclear energy, told some of the history of the atomic piles and uses of the products thereof. He described some of the effects of an atomic bomb, with special emphasis on debunking some of the scare stories put out by a few of the reporters with over-active imaginations while not minimizing the destructive power of such a blast.

The program was concluded with a showing of the movie, "Operation Crossroads."

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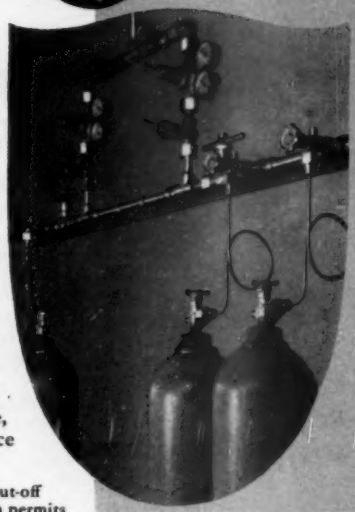

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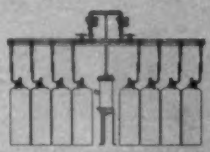

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Milwaukee

The December meeting of this Section was held on December 15th at the Ambassador Hotel. The technical speaker was Emory Garriott, one of the members of the Section who has spoken before many Sections of the Society. His subject was "Maintenance and Fabrication Welding with Bronze Electrodes."

Mr. Garriott discussed the A.W.S. classification system for nonferrous electrodes and explained the fundamentals of how to select the proper nonferrous electrode for various kinds of welding jobs. Preheating, joint preparation and interpass temperatures were discussed in relation to nonferrous welding in general.

Some of the basic requirements for success in making overlays for wear resistance with certain of the nonferrous electrodes was also discussed and illustrated with slides. Numerous applications of nonferrous electrode welding in the industry were presented.

The coffee talk was given by James Moher, Battalion Chief Executive Officer Civil Defense. The talk was aimed at developing an awareness of how serious an atom bomb explosion could be in a city the size of Milwaukee.

The regular meeting dates for the remainder of the season are as follows: March 30th, April 27th and May 26th (Annual Party).

New Jersey

The first annual conference of the Technical Societies Council will be held the afternoon and evening of Monday, March 12, 1951, in the Mosque Theatre, Newark, N. J. Subject: Power, its Source and Transmission.

The December meeting of the New Jersey Section was held in the Terrace Room of the Essex House, Newark, N. J., on December 19th. A 6:30 dinner preceded the meeting with 45 members and guests attending.

The regular meeting started with a showing of a film furnished by Esso Standard Oil Co., entitled "Marine Life Under Water." The speaker for the evening was J. P. Coughlin, Manager of Field Application Engineering, Arc Welding Dept. of Westinghouse Electric Corp., Pittsburgh, Pa. He gave a very interesting talk, illustrated with slides, outlining the past, present and future of welding.

Gordon Baker, Chairman of the New York Section, and F. B. Schilling, Chairman of Niagara Frontier Section, were welcome guests at this meeting, as well as two students, Nelson Laydner and Gustave Boog, from Brazil, who will be in the United States for three months as a result of the Inter-American Alliance to study and observe welding methods in this country.

New York

The New York Section held its December meeting on the 12th at Schwartz's Restaurant, 54 Broad St., New York.

The after-dinner speaker was Martin F. Carey, Special Agent of the Federal Bureau of Investigation. Mr. Carey spoke on the general activities of the

F.B.I. and described several of the interesting episodes which have occurred in the activities of the Bureau.

The technical speaker at this meeting was Cecil C. Peck, President, Cecil C. Peck Co., Cleveland, Ohio. His subject was "Tooling for Automatic Arc Welding," and his talk was illustrated by numerous slides. Mr. Peck clearly indicated by the examples shown in his slides how many welding operations may readily be mechanized and put on a production basis. It was evident from the many varied examples of production operations that automatic Arcwelding has become a significant factor in metal product fabrication.

During the showing of the slides Mr. Peck spoke informally and answered the audiences questions on the machines shown.

Left to right are Gordon Baker, Chairman, N. Y. Section, AWS; Speaker Cecil C. Peck; Technical Chairman A. N. Kugler, and Coffee speaker Martin S. Carey of the F.B.I.

A. N. Kugler, Mechanical Engineer, Air Reduction Sales Co., served as Technical Chairman of the meeting. Meeting arrangements were made by J. I. Medoff, Associate Member of the Executive Committee of the New York Section.

Pascagoula

The December 13th dinner meeting with an attendance of 26 was held at the Pascagoula Country Club. Dinner speaker was W. D. Pelan who spoke on the self interest advanced by A.W.S. membership.

Speaker at the technical session was R. H. Macy who spoke on Welding in Argentina. Argentine music records were played during dinner.

Peoria

The December dinner meeting was held on the 20th at the Jefferson Hotel, Peoria, Ill. Speaker was W. G. Moehlenpah, whose subject was "Resistance Welding—The Second Best Method of Fabrication." A movie "This is Resistance Welding" was shown in conjunction with the talk. Both were well received.

Philadelphia

The December meeting, held on the 18th, despite its close proximity to Christmas, was well attended and very successful.

Three of the local members did a fine job of presenting the material for the "Inert-Gas-Shielded Arc-Welding Symposium." "Consumed Electrode Type Welding" was covered by A. B. Gordon, The Linde Air Products. "Nonconsumed Electrode Type Welding" was covered by Walter Mehl, Heintz Mfg. Co., who illustrated his subject with slides of operations and setups in his shops. "Various Types of Equipment Used to Supply Welding Current" was covered by M. W. Brewster, Westinghouse Electric Corp.

All phases of inert-gas-shielded arc welding were given consideration by the speakers, who ably showed the scope and possibilities that lie within the various methods covered by this type of welding. The discussion brought out much new and interesting data and facts concerning this type of welding. This was to be expected of a subject as new as this one and full advantage of this was taken by our discussion leader, R. S. Phair, the Budd Co. As usual, informal discussion continued after the close of the regular meeting while the members enjoyed the refreshments that were set out.

The members who came early for dinner before the meeting were privileged to see the sound color film "Flying Fisherman" which was presented through the courtesy of Eastern Airlines. It was very enjoyable.

Preliminary reports on the membership drive show that progress is being made and momentum is being gained. Let's all get behind the drive and push the Philadelphia Section ahead.

The Philadelphia Section has always prided itself on presenting useful-up-to-date information to its members. The year 1951 is no exception. On March 19th "Titanium Metal and Alloys Containing Titanium" will be discussed by Earl Larsen, P. R. Mallory and Co., Inc. No subject could be more timely.

The Panel meeting on March 2nd will discuss the "Prevention and Control of Cracking Due to Welding." This is a natural follow up to the Panel meeting in February on "Allowances for and Control of Shrinkage Due to Welding." that was extremely popular.

Make plans now to be at these two outstanding meetings in March.

A Question and Answer Competition will be held on February 19th by the Philadelphia Section.

Rochester

The Rochester Section dinner meeting was held on Thursday, December 14th, at the Century Sweet Shop.

The program opened with the showing of a movie entitled, "Shark on the Line."

Richard D. Bourtros, Chief Engineer of Mixing Equipment Co., Rochester, N. Y., gave a very good lecture, with visual aids and models on the design and problems in the manufacturing of mixing equipment. Mr. Bourtros pointed out the fact that although they design the machine, the customer designates the material that will

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meet their corrosion or abrasive requirements. This tends to make some very complicated fabrication problems.

The turn out of about 40 members included a number of new members. Rochester reports that it is progressing very well with its membership drive.

Susquehanna Valley

The December 6th dinner meeting was held at the Berwick Hotel, Berwick, Pa. Dinner speaker was Lewis Russell who told about the new A.W.S. technical Literature. Technical speaker was R. T. "Bob" Gillette, Consulting Engineer of Schenectady, N. Y., who spoke on the subject of "Resistance Welding" and prefaced the technical portion of his discussion with history and description of the early patents of this welding technique. He described the various types of resistance welding. The proper preparation of materials to be subjected to fusion without a filler metal were carefully described. The effects of current, resistance and time were reviewed with the aid of slides. Proper maintenance and safety, as well as the physical constants of various metals were given attention by the speaker. He stressed the need of establishing a heat balance in joining dissimilar metals by the resistance-welding process.

The January 3rd dinner meeting was held at the Berwick Golf Club, Berwick, Pa. Coffee speaker was John Hewitt of the Wilkes Barre Auto Hill Club. His talk was very well received.

Technical speaker was R. J. Metzler, whose subject, "Design for Silver-Alloy Brazing" included a movie on brazing and welding of aluminum, all of which were highly interesting.

Syracuse

The September meeting was held at the Hotel Onondaga, Hiawatha Room, on Wednesday, September 13th. Following the dinner at 6:00 P.M., a coffee speaker was present to discuss briefly the Red Cross Blood Donor Program.

Paul W. James of the San-Equip. Co. acted as technical chairman for the evening and also gave a short talk on the subject: Automatic vs. Hand Welding. He then introduced Ivar W. Johnson of the General Electric Co. Mr. Johnson spoke on Slope Control in Resistance Welding.

Following a film entitled "This is Resistance Welding," Mr. Johnson covered the fundamental equation of resistance welding, factors affecting the resistance welding of metals, and factors to consider in joint design.

After drawing for a door prize the meeting was adjourned and coffee and sandwiches were served. The host for the meeting was the San-Equip. Co.

The October meeting was held on the 11th in the Hiawatha Room, Hotel Onondaga. Following the dinner, William Ogden, Merchants Bank, gave a short coffee talk on the subject of "Wills."

Following a motion picture film "Wild Fowl in Slow Motion," Edgar G. White of the Crouse-Hinds Co., acted as technical chairman of the evening, introducing R. E. Ballentine of the Westinghouse Electric Corp.

Mr. Ballentine spoke on "Phosphorus-Bearing Alloys for Low-Temperature Brazing." Covered in the discussion were such points as brazing, definition of; metallurgical principle of phos-copper alloys and the physical characteristics thereof, and the principles of usage and proper application of the phos-copper alloys.

After drawing for a door prize the meeting was adjourned and coffee and sandwiches were served. The host for the evening was the Crouse-Hinds Co. of Syracuse, N. Y.

The November meeting was held on the 8th also at the Hiawatha Room of the Hotel Onondaga. Coffee speaker was Director of Civilian Defense Harvey Smith, who spoke for an hour on Civilian Defense and what we can do to help ourselves in event of atomic bombing. Mr. Smith outlined the current world situation with a short geo-political discussion and reference to maps and charts. His discussion of the atomic bomb was forcefully put across in conjunction with motion picture films of the effect of the bomb on Hiroshima and Nagasaki.

Robert I. Hicks of the Lamson Corp. acted as technical chairman for the evening and introduced Paul W. James, Vice-President and General Manager of the San-Equip. Co. of Syracuse, N. Y.

Mr. James discussed "The Pros and Cons of Submerged-Arc Welding." He pointed out the definite need for more study on jobs before they are consigned to Automatic welding. By using hand-welding operations with large diameter electrodes and proper techniques, it was pointed out that quite frequently hand welding is more economical than automatic submerged-arc welding which often requires more floor to floor time because of elaborate and costly jigs and fixtures and set-up time. He cited a number of examples in various industries where a close study had returned the job from automatic welding to hand welding with greatly increased production.

After drawing for a door prize the meeting was adjourned and coffee and sandwiches were served. The host for the evening was the Lamson Corp. of Syracuse, N. Y.

The meeting was held in conjunction with the members of the American Society for Metals and there was a total attendance of approximately 150.

Washington

The Washington Section held their monthly meeting on December 19th at the Engineers Club of Washington, Inc. Dinner preceded the meeting. C. H. Jennings of Westinghouse Electric Corp. presented a clear-cut illustrated talk on "Developments in Arc-Welding Equipment." A lively discussion followed the meeting. Refreshments were served.

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abstracts of CURRENT WELDING PATENTS

prepared by V. L. Oldham

Printed copies of patents may be obtained for 25¢ from the Commission of Patents, Washington 10, D. C.

2,530,487—Portable Arc Welder—Keith Thorndyke, Pipestone, Minn.

Thorndyke's patented arc welder includes a base plate engagable with a support article, and a welding generator is mounted on the base plate. A reactor coil is carried by the base plate and a terminal plate is also provided in the welder and carried by the base plate.

2,530,969—Welding Clamp—Robert A. Johnson, Longmeadow, Mass.; Effie B. Johnson, executrix of said Robert A. Johnson, deceased, assignor to Westfield Metal Products Co., Inc., a corporation of Massachusetts.

This patent relates to apparatus for holding adjacent ends of aligned bars of stock in bar-supporting tubes in adjacency for welding operations.

2,531,005—Venting Plug for Welding Rods—Belmont D. Smith, Eaton, Ohio.

This patent covers the combination of a hollow welding rod with a coating of welding flux on the outer surface of the rod. The welding rod has a plug carried at the open end of the rod, and this plug includes means for venting gases from the rod when it is heated.

2,531,006—Tip for Cutting Blowtorches—Elmer H. Smith, Minneapolis, Minn., assignor to Smith Welding Equipment Corporation, Minneapolis, Minn., a corporation of Minnesota.

This cutting tip for blowtorches includes an outer member having an enlarged axial bore therein, with the wall of the bore tapering inwardly to a restricted cylindrical bore and terminating in an outwardly tapering bore at the discharge end of the tip. An inner member is provided and has a portion fitting in the enlarged bore and having a reduced cylindrical end portion fitting in the restricted cylindrical bore and extending into the outwardly tapering terminal bore of the outer member. Preheating orifices are formed between the inner and outer members and the outwardly tapering terminal bore forms an expansion chamber for the preheating flame.

2,531,174—Hole Burning Torch Tip—Emile A. Tomlin, Cambridge, Mass.

Tomlin's torch tip includes a body member having an axial passage therethrough for receiving oxygen from a torch. A series of oxygen outlets are provided spaced equal distance from the center of the tip member, while an annular series of combustion gas passageways are formed through the tip member and surrounding each of the oxygen outlets. A special pivot-point member is located in the center of the tip member and extends outwardly from the front face thereof.

2,531,181—Automatic Arc-Welding Apparatus—Wayne E. Wilson, Fitchburg, Mass., assignor to General Electric Co., a corporation of New York.

This automatic arc-welder includes means for lengthwise feeding a continuous column of unit-length electrodes welded together in end-to-end engagement. Various means are provided in the apparatus for storing a plurality of unit-length electrodes, for moving them into end-to-end contact in the apparatus and for feeding them from the apparatus. Special means are provided for welding a unit-length electrode to the continuous electrode in and being fed by the welding apparatus.

2,531,450—Arc-Oxygen Underwater Cutting Torch—Zygmunt A. Machiewicz, United States Navy.

This special patented torch includes a length of pipe having an electrical connection provided thereon with a standard T fitting at one end with a threaded bushing in one end of the fitting and a piece of oxygen hose in the other end of the fitting. A standard elbow is included in the apparatus and it connects to the projecting end of the oxygen hose while the other end of the elbow has tubing extending therefrom with an oxygen valve being provided therein. A clamping screw extends through the T and the bushing therein for clamping a tubular electrode thereto.

2,532,410—Inert Monatomic Gas-Shielded Metal-Arc Welding Process—Harry E. Kennedy, Berkeley, Calif., assignor to The Linde Air Products Co., a corporation of Ohio.

Kennedy's welding process comprises applying a low voltage of the order of 20 to 28 v. across a bare metal welding rod and metal work composed of a refractory-oxide metal selected from the class consisting of aluminum, stainless steel and magnesium. A stream of inert monatomic gas is discharged around the end portion of the rod and against the work. The rod is fed towards the work at a substantially constant relatively rapid rate and the rod and gas stream are moved along a desired path to be welded on the work while maintaining substantially constant both the rapid rate of rod feed and the low voltage used.

2,532,411—Constant-Potential—Submerged-Melt Metal-Arc Welding—Harry E. Kennedy, Berkeley, Calif., assignor to The Linde Air Products Co., a corporation of Ohio.

This welding system or apparatus includes a constant potential source of welding current having a capacity of the order of 19,000 to 81,000 amp. per square inch of welding rod cross section, and other means are furnished for feeding a bare welding rod at a constant rate toward a welding zone. Welding current of the density indicated is provided to the welding rod and forms a welding arc. Other means supply a granular flux medium to the welding zone to stabilize the welding arc whereby for a rod of given size the amount of welding current inherently depends upon the speed at which the rod is fed toward the welding zone, and the resulting welding operation is very stable.

2,532,807—Arc-Welding Torch—Dean C. Girard, San Leandro, and Frank T. Roach, Hayward, Calif., assignors, by mesne assignments, to National Cylinder Gas Company, a corporation of Delaware.

This patent relates to a combination in an arc-welding system wherein the arc is a low-frequency alternating-current arc and is stabilized by high-frequency current of the order of several megacycles superimposed thereon. The combination includes an inductor in as close proximity to the arc as it is physically possible, and circuit means consisting of a nonradiating transmission line for connecting the inductor for energization across the high-frequency current source provided.

2,532,825—Welding Control System—Herbert F. Storm, Schenectady, N. Y., assignor to General Electric Co., a corporation of New York.

2,532,826—Welding Control System—Charles R. Hewson, Altamont, N. Y., assignor to General Electric Co., a corporation of N. Y.

These welding control systems relate to relatively complicated electrical connections for control of welding conditions. The controls include ignitrons the firing of which is controlled so as to increase the current supply to the welding load circuit in a predetermined manner.

2,532,852—Welder's Ear Guard—Vern George Oaks, Superior, Wis.

This patented guard includes a body portion composed of fine-mesh screen and having a substantially flat side surface. Flanged portions project inwardly from portions of flat side surfaces to overlap corresponding portions of an ear, and other means are secured to the central portions of the body portion for holding same in position on an ear. A pair of guards normally are connected by and supported by such means.

2,533,069—Electric Arc Welding—Joseph M. Tyrner, New York, N. Y., assignor to Air Reduction Co., Inc., a corporation of New York.

Tyrner's patent is on a dynamo-electric machine that includes a main armature winding, a field winding for controlling the amount of current supplied by the machine and means for supplying excitation current to the field winding. Other means are inductively related to the armature winding and operatively connected to the means for supplying field excitation current to cause same to change in inverse ratio to the armature current. This means comprise an auxiliary armature winding mounted on the armature and responsive to the armature reaction flux of the machine, and a stationary coil inductively related to the auxiliary armature winding.

2,533,353—Starting-Rod Feed Device for Gas Torches—Edward J. Flynn, Jersey City, N. J., assignor to Air Reduction Co., Inc., a corporation of New York.

A starting-rod feed device for gas torches is disclosed in the patent. Such device includes a clutch collar having an opening for receiving the starting rod, and means for reciprocating this collar forwardly and rearwardly lengthwise of the starting rod for positively canting the collar to cause the rod to move with the collar during the forward stroke of same.

2,533,605—Apparatus for Welding Slabs—Paul M. Mueller, Rome, N. Y., assignor to Blaw-Knox Co., Pittsburgh, Pa., a corporation of New Jersey.

In the patent, a machine for welding successive slabs of metal into a continuous plate and finah trimming the plate during the uninterrupted movement of the slabs and plate is provided. The apparatus includes a vehicle adapted to reciprocate between two substantially fixed points in a line along which the slabs and plate move in endwise relation, and a pair of transoms and crossheads are provided for preforming operations on the slabs as they move along the line. Clamps also are provided for securing the slabs in desired relationship as they are welded together.

LIST OF NEW MEMBERS

December 1 to December 31, 1950

ARIZONA

Dyer, J. D. (B)

BOSTON

Mc Namara, Thomas H. (C)
Morrison, Albert D. (C)
Teague, Lendall K. (C)

BRIDGEPORT

Barker, J. E. (C)

CHATTANOOGA

Downs, Bernard W. (B)

CHICAGO

Plichta, Henry F. (C)

CLEVELAND

Hand, Charles C. (B)
Nagy, Zigmond, Jr. (B)

COLUMBUS

Wilson, Clyde W. (D)

DALLAS

Woodard, James E. (D)

DAYTON

Brunton, Paul (C)
Marvel, Paul (D)
Meszko, Raymond P. (D)
Schulmeister, H. Fred (C)
Simmons, L. A. (D)

DETROIT

Anderson, Richard V. (B)
Archer, Norman B. (B)
Blackford, Lester J. (B)
Bronson, Charles A. (B)
Campbell, Fred C. (C)
Haldeman, John M. (B)
Isaacsen, Barton E. (C)
Jeffries, Lloyd W. (B)
Lamont, William D. (C)
Ludwig, Walter D., Jr. (B)
Martin, Felix J. (B)
Marx, Harry Z. (B)
Moody, Paul B. (B)
Rash, Luke (C)
Rebh, Robert P. (C)
Syme, Frederick W. (C)
Wheeler, Neil E. (B)
Woodward, Allen (B)
Wright, Rector (B)

EASTERN ILLINOIS

Kromp, Carl (B)

HARTFORD

Parker, Robert T. (C)

HOUSTON

Bennett, Howard W. (B)
Hamner, Robert J. (C)
Hoffman, Joseph H. (C)
Hollingshead, Chester D. (C)
Jones, Hugh R. (B)
Steward, Jack (B)
Zwerneman, James A. (B)

INDIANA

Gillespie, Joseph (B)

LEHIGH VALLEY

Seifert, Cary Lester (C)

LONG BEACH

Pope, Frank (C)

LOS ANGELES

Anderson, Robert E. (C)

NEW JERSEY

Joyce, Arthur T. (C)
Madsen, Stanley Walter (C)
White, William H. (B)

NEW YORK

Band, Alexander (B)
Borneman, Adam (C)
Gordon, Joseph (B)
Greb-Lasky, Frank (B)
Jacobsen, Edwin H. (C)
O'Reilly, Eugene J. (C)

NIAGARA FRONTIER

Drury, George (C)
Lark, Joseph H. (C)
O'Donnell, M. T. (B)

NORTHERN NEW YORK

Straub, Arthur J. (D)

NORTHWEST

Anderson, Malcolm (C)
Richmann, Le Roy (C)

PEORIA

Brugioni, Delmo Lloyd (C)
Swearingen, William J. (C)

PHILADELPHIA

Crawford, W. R. (C)
Duvall, Paul F. (B)
Jessar, Fred (B)
Matheny, James H. (B)
Mc Cauley, Edward R. (C)
O'Connell, Maurice W. (C)
Schwartz, Walter R. (C)

PITTSBURGH

Bergquist, T. W., Jr. (B)
Werner, Ephriam (B)

RICHMOND

Roberts, Paul S. (C)
Sherman, W. P. (C)
Thomason, Lawrence N. (B)

ROCHESTER

Fuhrman, George (C)
Gordon, Gilbert (C)
Jancs, Enos B., Jr. (C)
Mostyn, Henry (C)

SALT LAKE CITY

Croxford, Dale Leo (D)
Downey, Jim C. (D)
Hodgson, Edgar D., Jr. (B)
Lindbeck, Fred L. (D)
Lott, Keneth (B)
Plant, Morgan (D)
Steele, Lowell R. (D)

ST. LOUIS

Winkler, O. J. (C)

SUSQUEHANNA VALLEY

Sheffer, John W. (B)

SYRACUSE

Marches, Charles (C)
Wozniczka, John (C)

WESTERN MICHIGAN

Welcomb, James A. (C)

WICHITA

Brown, Charles (C)

WORCESTER

Latini, Albert J. (B)
Lukas, Joseph (B)
Polaski, Joseph (C)

MEMBERS RECLASSIFIED

LOS ANGELES

Fenlason, A. L. (C to B)

NEW JERSEY

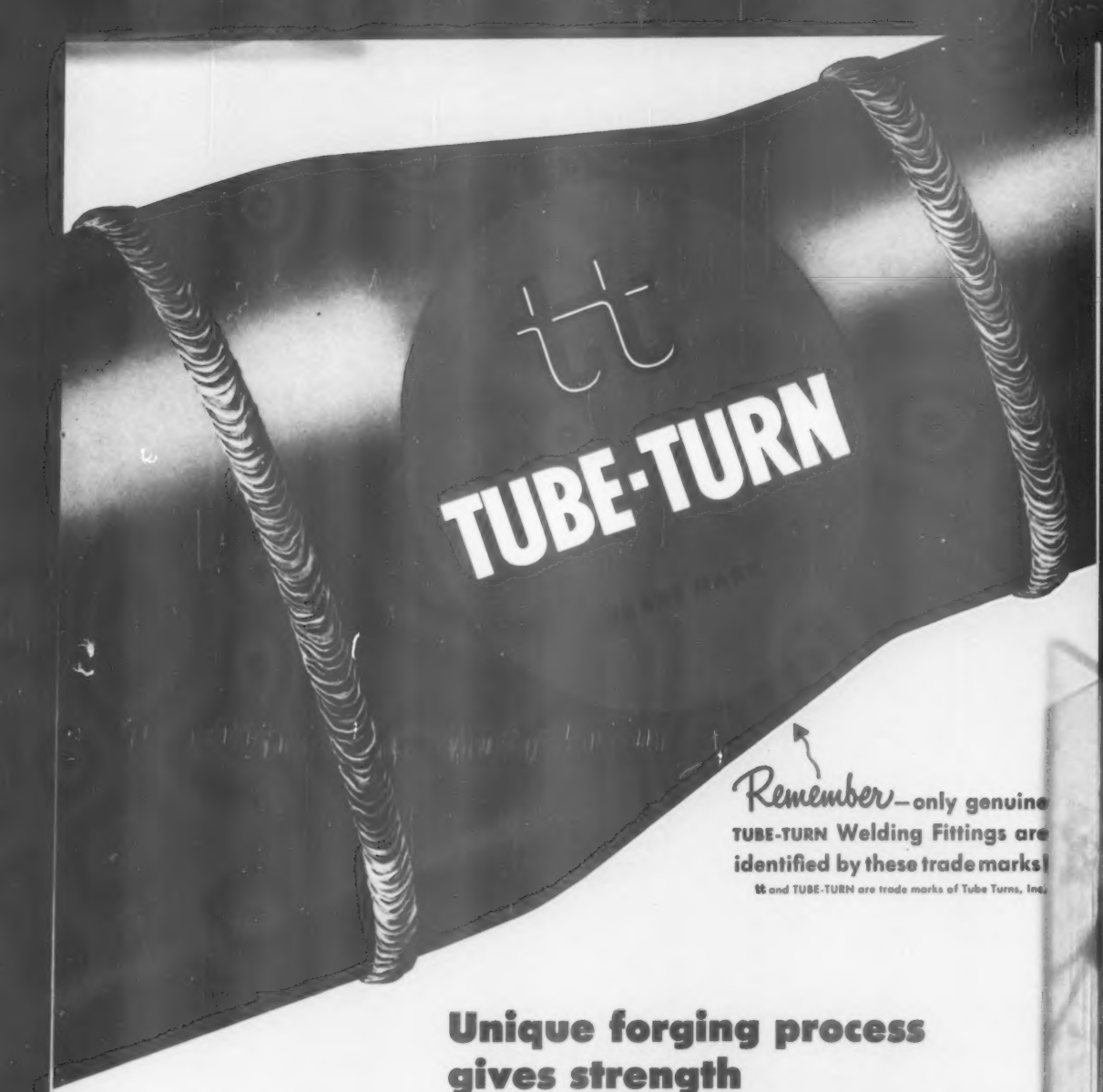
Forgett, V. (C to B)

NORTHWEST

Schwabe, Roy T. (C to B)

WICHITA

Hughes, Delbert E. (C to B)



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Wrought iron downspout welded on Cleveland Bridge

Cleveland's Abbey Avenue Bridge is big enough to collect a lot of water in a rainstorm. To get rid of it, nine downspouts, ranging from 39 to 66 feet in height, carry the water to underground sewers. Wrought iron pipe was chosen for its resistance to rust. All-welded piping was chosen for long life, structural rigidity. Weak joints here could be fatal!

Contractor on this job, the Gorman-Lavelle Company of Cleveland, likes speed on the job. The piping itself was quickly fabricated on the ground, then raised into position. Piping went up fast—and safely!

A lateral connection near the top of the spout leads up to drains on the bridge. TUBE-TURN Welding Reducers are installed at the very top of the spout. The drain sets in the reducer with a loose fit, allowing 2" clearance all around. Thus the bridge can expand on its roller bed without disturbing the pipe. TUBE-TURN Welding Reducers, Elbows, Laterals and Caps are all part of the leading line in welding fittings.

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Note TUBE-TURN Welding Elbow at bottom of downspout. Drain pipe will be connected to underground sewer. Underground, too, a system welded with TUBE-TURN Welding Fittings is the best long-term bet—there are no threads to corrode, the problem of leakage is eliminated.

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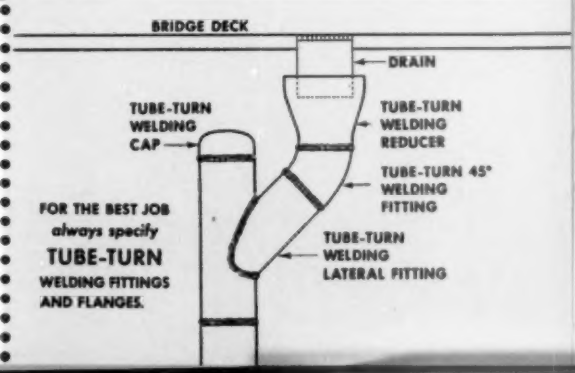
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American Society of Mechanical Engineers, and Society of Naval Architects and Marine Engineers

Supplement to The Welding Journal, February, 1951

Summary of the Work of the Ship Structure Committee

by Rear Admiral K. K. Cowart

SINCE 1946, the Ship Structure Committee with the cooperation of the AMERICAN WELDING SOCIETY, has arranged a Symposium on Ship Structure Research which is presented each year as a part of the Annual Meeting of THE WELDING SOCIETY in conjunction with the National Metals Congress. The Symposium this year, constitutes the fifth appearance of the Ship Structure Committee before this audience. I, personally, however, am a newcomer to these proceedings. I have only recently succeeded to the Chairmanship of the Ship Structure Committee and as Chairman, I feel privileged to perform this pleasant duty of opening the Ship Structure Symposium by addressing a few remarks to you.

Each year you have been told that the Ship Structure Committee is a cooperative effort of five member agencies to prosecute a research program for the improvement of the structure of ships. The member agencies consist of: the Coast Guard; the Navy (Bureau of Ships); the Maritime Administration of the Department of Commerce and the American Bureau of Shipping. This year, for the fifth agency, I have a change in membership to report. The Military Sea Transportation Service has replaced the Transportation Corps of the Army by virtue of the transfer of the Army's Transport Vessels to MSTS.

Part of the mission of the Ship Structure Committee is concerned with the dissemination of information pertaining to ship structure and it is in the implementation of this part of our mission that these annual sessions on Ship Structure Research are presented. In the further implementation of the mission, certain publications and reports have been issued from time to time. The first of these was the Final Report of the Board to Investigate the Design and Methods of Construction of Welded Steel Merchant Vessels, the predecessor of the Ship Structure Committee. This report was issued in July 1946. In addition, a technical Progress Report was issued in March 1948, supplementing the Final Report just mentioned. A second Progress Report is about to be released.

It is my intention this morning to summarize for you the salient points of this new report and to integrate them with the past work of the Ship Structure Committee and the Board.

To summarize briefly, and in so doing to indicate the magnitude of the problem involved—there were approximately 5000 merchant ships constructed during the war and of this number about one-fifth, or 1000 ships suffered cracks in varying degrees. Of these, 190 ships sustained fractures which were classified as serious. Some cases involved the loss of lives and ships. The total cost to the nation of these casualties is almost fifty million dollars.

During the last two years only one additional welded merchant vessel has sustained a complete failure by breaking in two. This was a T-1 Tanker, the S.S. *Capitan*, which came to grief during the night of Dec. 24, 1948 off Cape Hatteras. This was the first instance of a major casualty in this class of vessel, although 29 were built and placed in service. As is usually the case, the casualty was associated with low temperature. The air temperature was 35° F. and the sea was rough.

During the same two-year period, 32 structural fractures were sustained by welded steel vessels, so that it

Rear Admiral K. K. Cowart is Engineer-in-Chief of the U. S. Coast Guard at C.G. Headquarters, Washington, D. C.

Presented at Thirty-first Annual Meeting, A.W.S., Chicago, Ill., week of Oct. 22, 1950.

can be easily seen that the fracture menace has by no means been completely eliminated. It is believed, however, that the threat of major structural failures resulting in vessels breaking in two has been successfully met and largely overcome by the institution of structural alterations in existing vessels and by changes in design and fabrication practices in the case of new construction. The structural alterations referred to are mainly riveted crack arrestors and hatch corner modifications. The record of the so-modified Liberty ships and T-2 Tankers bears witness to the success of this program. The Victory ships which have suffered no major structural casualty to date, are ample testimony for the success of the program of change in design for new construction.

From the very beginning, the work of the Board, as well as the Ship Structure Committee, has been classified under the broad headings of Design, Materials, Fabrication and Statistical Analysis of Ship Fractures.

The work of statistical analysis was started as soon as the Board was convened and has continued to the present time. This work embraces all structural casualties exclusive of those resulting from war damage, which have occurred since October 1942. This study has resulted in clearly establishing that the fracture menace arose primarily from the combined effects of notches and notch sensitive steel. It also emphasized the importance of the performance of structural materials at low temperature.

As might be expected, the wartime research work sponsored by the Board was of the "shotgun" variety with a very practical slant, and was concerned primarily with Design and Fabrication. Quick answers were desired, even though only partial solutions, and expense was not an important factor. The results of this program are seen in the installation of riveted crack arrestors in existing all welded vessels and in the improvements in design and fabrication practices as exemplified in the Victory ships previously mentioned.

After the close of the war, the research program was reoriented to a more fundamental or "basic" approach to the problem of brittle fracture of steel. As a result, studies in the Material category were emphasized, because it became apparent that without a full understanding of the physical and metallurgical facts underlying the behavior of metals and the mechanism of fracture, it would be impossible ever to achieve the elimination of riveted seams or crack arrestors and the consequent realization of the all-welded ship with its many inherent advantages. The program of research in materials has served to further emphasize the culpability of notch-sensitive steel in ship fractures and has been directly responsible for the establishment by the American Bureau of Shipping of a new specification for shipbuilding steel which has as its objective the procurement and utilization of more notch tough steels in shipbuilding construction. However, inasmuch as the American Bureau of Shipping specification is based on chemical composition rather than physical behavior, the Ship Structure Committee is continuing its efforts to evolve and establish a "*performance*" specification and acceptance test for screening out notch-sensitive steels.

An interesting and significant finding of the "Materials" Program is that—"there is danger of brittle fracture in ship steel when, in the standard V-notch Charpy impact test, the energy absorption is less than 15 ft.-lb. at a temperature of 60° F. It is not known, however, how much greater the notch toughness of the steel must be to remove danger of brittle fracture."

I think it is important for all of you to keep in mind that the findings of the Ship Structure Committee's program and the broad concepts and principles established from them apply with equal force to all types of welded structures, whether they be Buildings, Bridges, Pressure Vessels or Ships.

Within the last year the program of research in the Design category has been accelerated somewhat and studies are now being made on such subjects as "The Reinforcement of Openings in a Ship's Hull," "The Establishment of Suitable Design Principles for Cargo Hatch Corners," "The Interaction between the Superstructure and the Main Hull Girder of Ships" and "The Effectiveness of Typical Structural Details of Ships." You will hear some of this work reported in this Symposium today.

One of the first research efforts sponsored by the Board was a study of residual or "Locked-in" stresses in an attempt to establish their origin, magnitude, distribution and importance. This was a most logical step at that time, since it was widely believed that the fracture difficulties being experienced were due entirely to residual or "Locked-in" stresses. The study was extensive and complete. The results are clear and well substantiated. They are listed in the Final Report of the Board and caused the Board to conclude that " 'Locked-in' stresses do not contribute materially to the failure of welded ships." This conclusion was so directly contrary to the thinking of many in the shipbuilding, structural and welding industries that it has not been accepted by all. It is significant, therefore, to state at this time, that this conclusion, in so far as research studies and experimental evidence are concerned, remains valid today, and while there is considerable experimental evidence to support it, none has been produced to refute it. Nevertheless, because of the continuing controversial nature of the subject, the Ship Structure Committee has requested the National Research Council to establish a committee of outstanding, unquestioned and unprejudiced authorities in this field, from this country and abroad, to review all of the material available on this subject and to prepare a Monograph for publication by the Ship Structure Committee. It is hoped that the proposed Monograph will lead to a resolution of this controversial issue.

The terms of the fracture problem as I have presented them and the effectiveness of riveted seams and crack arrestors in stopping fractures, might be thought to indicate a logical return to the all-riveted ship. It should be kept in mind, however, that many advantages are inherent in the welded ship which cannot be realized by

riveting and which are translatable to savings in construction and operating costs. Some of these are:

a. Speed of construction. This is particularly important in the event of a "National Emergency."
b. Weight saving, resulting in greater cargo carrying capacity.
c. Tightness of hull and tanks, which is easy with welding—difficult with riveting.
d. A smoother hull with consequently lower resistance, hence more speed with the same power.
e. The ease with which qualified welding operators can be trained as compared with riveters; utilization of women welders.
f. Lower cost of fabricating equipment for welding.
g. Portability of equipment for welding.

The all-welded ship with its many advantages will ultimately be realized. This can happen, however, only through a concerted coordinated program of research. The Ship Structure Committee is carrying on such research in the field of Design, Materials and Fabrication. Careful coordination of these programs is assured through the utilization of the technical advice and coordination services of the National Academy of Sciences.

A measure of the effectiveness of the Ship Structure Committee's program is seen in the record of the Victory ships, previously mentioned, which incorporated the design and fabrication improvements which had been evolved at that time. Whereas the incidence of recorded failures for Liberty ships was 19.45 per 100 ship years, it is only 0.47 per 100 ship years for the Victory ships. The incidence of major fractures in the two types is 4.18 and 0.0, respectively.

In addition to the research program, the information obtained therefrom is being disseminated in order to educate all concerned in shipbuilding and obviate the recurrence of past mistakes, which in most cases, were made in ignorance. In the implementation of this purpose Monographs, Reports and research findings in general, are made available through wide distribution and publication.

The anticipated benefits of the Ship Structure Committee's program are summarized as follows:

a. Ships constructed of improved materials and making use of improved design and fabrication techniques, will be capable of operating anywhere in the world without danger of destruction or injury in the form of hull fractures. The lives of the passengers and crews of such vessels would not be in jeopardy from a hazard in addition to those of the natural forces of nature, such as wind and sea, to which ships are normally exposed. The lay-up time and the cost of repairs for such ships would be less.

b. The use of improved design, materials and fabrication techniques will simplify construction practices and reduce the construction costs. In particular, the need to repair cracks occurring during construction will be removed.

c. A reduction in ship-operation costs should result from a reduction in maintenance and repair expenses and the elimination of cargo loss and demurrage.

d. Ships with hulls of improved construction will in time of war when used as Naval Auxiliaries, be better able to withstand the forces of underwater explosion and attack by missiles.

e. New and unconventional ship designs which might be visualized for the future could be undertaken with confidence gained through a better understanding of the fundamental principles covering design and fabrication procedures.

f. In the event of war or National Emergency, there would be available a practical selection of noncritical shipbuilding materials and up-to-date instructions for the design and construction of ships.

g. New structural design concepts in shipbuilding would be evolved which would take into account concepts of plasticity and the properties of energy absorption and notch sensitivity.

The all-welded ship will be realized. If I may borrow the words of Sir Charles Lillicrap, Great Britain's Director of Naval Construction—"..... I look forward to the day when ships of all kinds will be entirely 'innocent' of rivets."

The Ship Structure Committee is indeed grateful to the AMERICAN WELDING SOCIETY for having provided an entire day in their crowded program for this Annual Meeting, to make possible the presentation of this Fifth Symposium on Ship Structure Research. We look forward with pleasure to continuing cooperation between the Ship Structure Committee, the AMERICAN WELDING SOCIETY and the Welding Research Council in the solution of our mutual problems in the field of Ship Structure Research.

Stress Studies of Welded Ship Structure Specimens

Tests showing stress concentrations and energy for fracture of four different types of intersections of a bottom longitudinal and a transverse bulkhead as found in welded tanker design.

by Wm. R. Campbell

Abstract

The paper presents the results of tests of four large ship specimens which were tested in the Engineering Mechanics Section of the National Bureau of Standards for the Ship Structure Committee. The tests are part of a study of typical structural discontinuities in ships to determine the magnitude of stress concentrations and areas affected by discontinuities, and to furnish data necessary for improving current designs. The elastic stress distribution at room temperature, strain distribution prior to failure at 0° F. and energy to fracture at 0° F. are presented for four interrupted longitudinal tensile specimens. Each specimen represents the intersection of a bottom longitudinal and a transverse bulkhead as found in welded tanker design. Stress values in the region of the intersection are compared with stresses in the longitudinal bordering the intersection. Stress concentrations and energy for fracture for the different specimens are also compared.

INTRODUCTION

A NUMBER of serious structural failures of welded merchant ships in 1943 led to the creation of a Board of Investigation to study the design and methods of construction of welded merchant ships. The Board compiled a large amount of design and service information during the war years and in 1946 in its final report[1] recommended the creation of a permanent organization to continue many of the Board's activities and to sponsor new and promising lines of research in the field of ship structures. The Ship Structure Committee was established for this purpose and presently sponsors and directs research in many phases of the ship structure field.

The Ship Structure Committee through the Bureau of Ships, Department of the Navy, requested the National Bureau of Standards to undertake a program of research on the stress distribution in bulkhead intersections and interrupted longitudinals as part of the Committee's general study of structural discontinuities. A Project Advisory Committee with representatives from the member agencies of the Ship Structure Committee was convened to formulate a program for the study of these design details. This program was authorized late in 1947 and was begun the same year.

The Advisory Committee designed and arranged to secure eight bulkhead intersection tensile specimens. Four represented intersections of a bottom longitudinal and a transverse bulkhead, and four represented intersections of a longitudinal bulkhead and a transverse bulkhead. The specimens represented structural details peculiar to the design of welded tankers.

This paper gives the results of tests on the four interrupted bottom longitudinal specimens. The elastic stress distribution at room temperature, the strain distribution prior to failure at 0° F., and the energy to fracture were determined experimentally for each specimen. The determination of the distribution of stress in the specimens consisted of loading the specimens and measuring the strains on a sufficiently large number of gage lines to determine the state of strain, from which the state of stress could be determined by standard methods. The test to failure of each specimen was conducted at 0° F. in an effort to duplicate the brittle fractures which have been characteristic of ship failures in many cases.

The procedures for testing were planned in the Mechanics Division of the National Bureau of Standards in cooperation with the Advisory Committee and were initiated by Dr. A. H. Stang prior to his retirement. During the course of tests many valuable suggestions were received from the Project Advisory Committee, consisting of Cmdr. James McIntosh U.S.C.G., Chairman, E. M. MacCutcheon, Jr., W. G. Frederick, John Vasta and Mathew J. Letich, and from Dr. Finn Jonassen who serves as technical advisor to the Ship Structure Committee.

INTERRUPTED LONGITUDINAL SPECIMENS

Description of Specimens

The four bottom longitudinal specimens, numbered 1 to 4, are shown schematically in Fig. 1. The specimens were fabricated from steel plates joined by welding. Each specimen represented the structural details of a bottom longitudinal at its intersection with a transverse bulkhead such as is shown in Fig. 2. As can be seen from Figs. 1 and 2, each test specimen consisted of a section of the hull plating, a length of the longitudinal stiffener, a section of the transverse bulkhead and the triangular bracket carrying the flange strength of the longitudinal through the bulkhead. The specimens were full size except for the thickness of the bottom plate, for which $^1/_2$-in. plate was used instead of $^9/_{16}$-in. plate.

Tensile Properties of the Material

Four samples were taken from the stock of $^1/_2$-in. plate before fabrication of the specimens. The tensile properties were determined from coupons machined from these samples. The coupons were A.S.T.M. 8-in. gage length tensile specimens for plates, shapes and flats. The width of the coupons at the reduced section was 1.5 in., and the thickness was that of the plate as rolled.

The coupons were tested in a hydraulic testing machine having a capacity of 100 kips. Longitudinal strains were measured by means of an Ewing extensometer of 8-in. gage length. One division on the scale of this instrument corresponds to a strain of 0.000025. Readings were estimated to 0.1 division. Transverse strains were measured by means of two SR-4 type A-3 resistance strain gages using an SR-4 portable strain indicator.

The yield point was determined by the drop of beam method. Young's modulus

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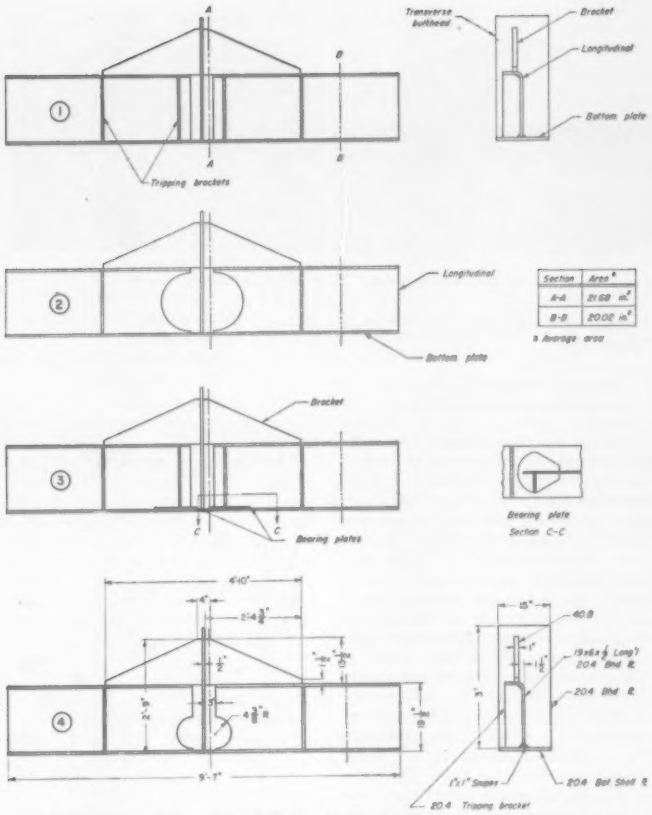


Fig. 1 Dimensions and details of interrupted longitudinal specimens

Fig. 2 Bottom longitudinal at intersection of transverse bulkhead in a T2 tanker

Longitudinal bulkhead at right. Transverse bulkhead at back. Bottom longitudinal in center between heating pipes. Bottom plate is below heating pipes. (Photograph from reference 2)

of elasticity and Poisson's ratio were determined by the method of least squares, and the proportional limit was determined from difference curves.

The tensile properties of the material are given in Table 1.

TESTING PROCEDURE

The interrupted longitudinals were loaded in a horizontal Emery testing machine having a capacity in tension of 1150 kips. Each specimen was placed in the testing machine with the web of the longitudinal horizontal and with the flange of the longitudinal upward. Specimen 1 was the first specimen tested, and the loads for this specimen were applied through two U-bolts at each end of the specimen. The legs of the U-bolts projected through four holes in plates welded to each end of the specimen. The throats of the U-bolts encircled the legs of a T-shaped fixture, the stem of which was gripped in the head of the testing machine. Two of the U-bolts failed during the low temperature test of Specimen 1. An attempt with new fixtures of similar design also resulted in failure. Consequently the U-bolt arrangement was discarded and pin loading fixtures were designed for the tests of specimens 2, 3 and 4. The load was applied to these specimens through reinforced welded plate headers which were connected by pins and straps to eyebars gripped in each head of the testing machine. Specimen 4 with built-up welded end tabs is shown in Fig. 3. Specimen 2 is shown in the testing machine in Fig. 4.

Elastic Tests at Room Temperature

SR-4 single element resistance strain gages, type A-3, and three element rosettes, type AR-2, were installed on each specimen in locations similar to those shown for specimen 4 in Fig. 5. Each gage number in Fig. 5 represents two gages, one on each end of the specimen in identical locations. Gages were air dried for 16 hr, heated with infrared lamps for 3 hr and waterproofed with a mixture of equal parts U.S.P. petrolatum and petrosene wax. Strain readings were taken on all gages at loads of 20, 60, 100, 140 and 160 kips. using SR-4 portable strain indicators.

Table 1—Properties of the Material at Room Temperature

Sample	*A*	*B*	*C*	*D*	*Avg.*
Thickness, in.	0.500	0.500	0.500	0.500	0.500
Young's modulus, ksi.	30,100	29,900	30,500	30,100	30,100
Poisson's ratio	0.287	0.289	0.293	0.292	0.290
Proportional limit,* ksi.	30.7	34.2	30.9	30.1	31.5
Yield point, ksi.	33.7	35.6	36.8	36.8	35.7
Tensile strength, ksi.	61.8	62.0	62.0	61.8	61.9
Elongation in 8 in., %	32.0	30.4	32.0	31.9	31.6

* 30×10^{-6} offset.

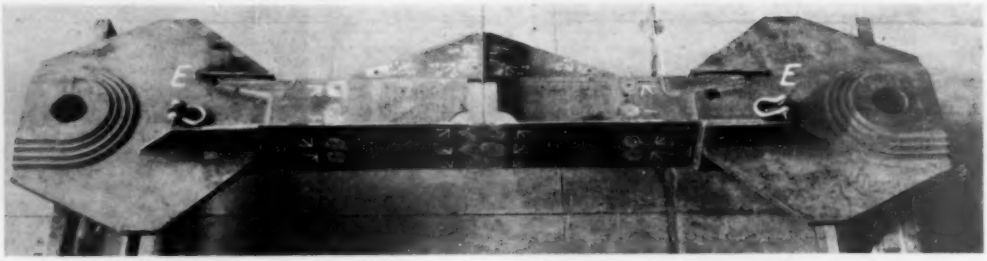


Fig. 3 Longitudinal specimen 4 showing welded pulling tabs. Diameter of pin hole is 7 in.

Tests to Failure at 0° F.

Following the test at room temperature, an open-top box of insulating board was built around the specimen as shown in Fig. 4. Solid carbon dioxide was used to cool the specimen to 0° F. Ten copper-constantan thermocouples were installed on each specimen for temperature determinations. Two over-all dial extensometers were attached to the pulling tabs for determining the center line extension of the specimen. The welds between the specimen and the end tabs were heated with infrared lamps during the test to reduce the possibility of failure of the end connections at low temperature.

During the low temperature test, strain readings were taken on all gage elements parallel with the axis of the specimen for use in determining the strain distributions on sections A-A and B-B, Fig. 1. The gage numbers of the gages read during the low-temperature test of Specimen 4 are underscored in Fig. 5. Strain gage readings were taken for load increments of 50 kips. up to a load of 200 kips. and thereafter at increments of 100 kips. until failure was imminent. Dial extensometer readings were taken up to the instant of failure.

STRESS DISTRIBUTION AT ROOM TEMPERATURE

The strains for corresponding gage lines on opposite sides of plates and in identical locations on the two ends of the specimens were averaged for each load. The load-average strain relationships were reasonably well represented by straight lines. Stresses due to a 160 kip. load were computed from the linear load-strain relationships by standard methods.[3,4] The magnitude and direction of the principal stresses at each rosette were computed using the average values of Young's modulus and Poisson's ratio given for the material in Table 1. Axial stresses for determining the stress distribution on sections A-A and B-B of each specimen, Fig. 1, were computed from the principal strains for a load of 160 kips. The ratios of the axial stresses to the average stress in the longitudinal outside the area of the intersection were computed to give a measure of stress concentrations. The average stress in the longitudinal (P/A_g) was taken as the load, 160 kips., divided by the combined area of the longitudinal and bottom plate at section B-B. The average axial stresses on the bracket and bottom plate were computed and multiplied by the bracket and bottom plate areas, respectively, to obtain the loads carried separately by these elements at section A-A. Similarly, the load carried on the longitudinal section B-B was computed for comparison with the load indicated by the testing machine. The position of the load line at section A-A was calculated for each specimen by the method given in the appendix.

The magnitude and direction of the principal stresses for the four specimens are shown in Fig. 6. Comparison of the principal stresses and their directions

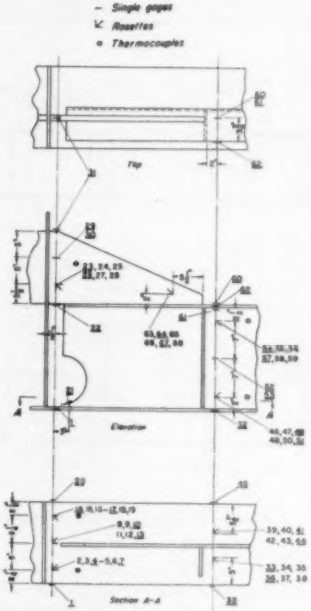


Fig. 5 Strain gage and thermocouple locations for specimen 4

Rosette gages are numbered clockwise. Strain gages were attached on both sides and both ends of each specimen

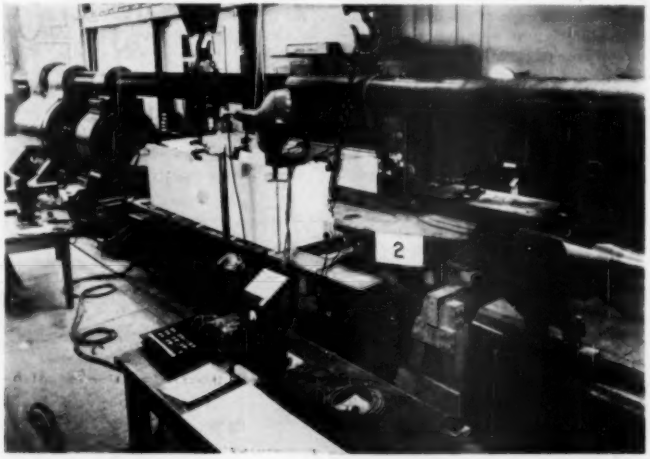


Fig. 4 Longitudinal specimen in testing machine for test at 0° F.

Pulling plates and eye fixture at right

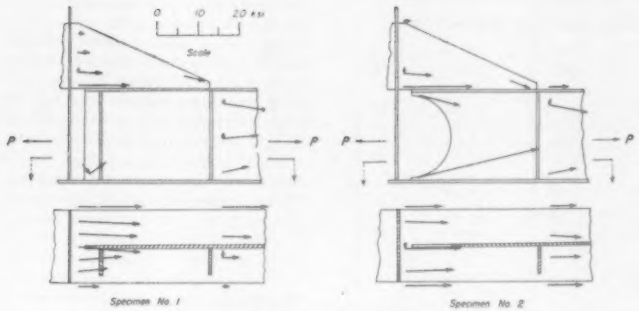
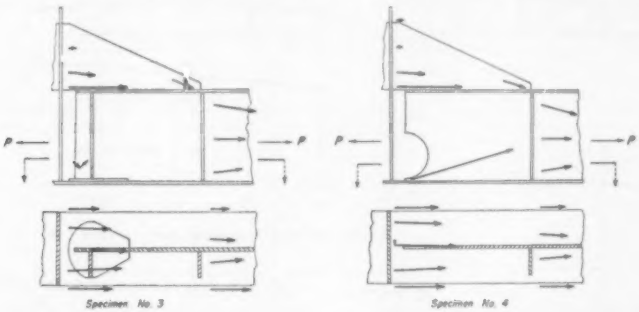


Fig. 6 Principal stresses for a load of 160 kips.

Minor principal stresses less than 1 ksi. are not shown

indicates that similar stress patterns were obtained for the different specimens. Major principal stresses on the bracket were directed toward the flange of the longitudinal. Bottom plate stresses showed some convergence on the web of the longitudinal and a definite unbalance across the width of the plate. This unbalance was greatest on Specimen 1, which was loaded through the U-bolt fixtures. Stresses in the web of the longitudinal showed convergence toward the load line. The highest stresses measured were located on the lower edge of the semicircular cutouts in the webs of specimens 2 and 4. Stresses in similar locations on specimens 1 and 3, without cutouts, were relatively small.

The axial stress distributions on the bracket and bottom plate of each specimen adjacent to the transverse bulkhead, section A-A, are given in Figs. 7 and 8. Figure 7 shows that the upper edge of the bracket was in compression on all four specimens. Axial stresses reached maximum values near the lower edge of the



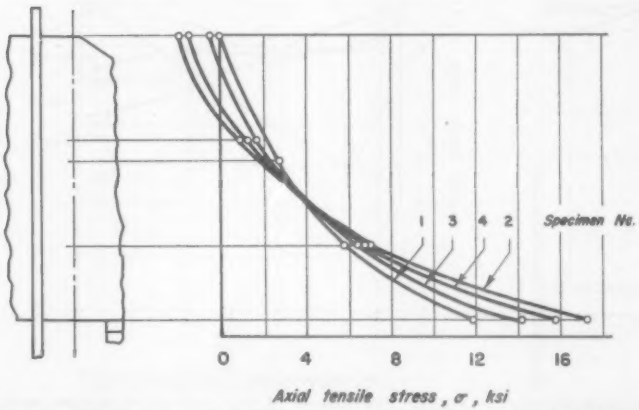


Fig. 7 Distribution of axial stresses on bracket, section A-A. Load 160 kips.

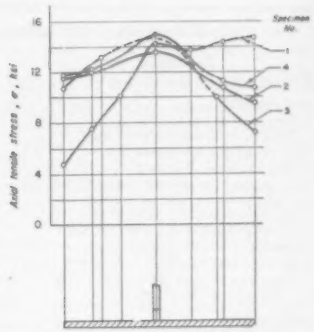


Fig. 8 Distribution of axial stresses on bottom plate, section A-A. Load 160 kips.

bracket. Stresses here ranged from 1.5 to 2.2 times the average stress in the longitudinal outside the intersection, and were slightly higher for specimens 2 and 4 with semicircular cutouts in the longitudinal than for specimens 1 and 3 without the cutouts.

Axial stresses on the bottom plate, Fig. 8, showed a distinct peak at the center of the plate in line with the web of the longitudinal. Stresses at this point were 1.7 to 2.0 times the average stress in the longitudinal. The stress distribution on the bottom plate of Specimen 1 reflected the unbalanced principal stresses previously mentioned. The stress distributions for specimens 2, 3 and 4 were similar in appearance and did not indicate any marked effects of differences in specimen design. Except for Specimen 1, stresses on the upper edge of the bottom plate (left, Fig. 8) were slightly higher than stresses on the lower edge. This stress gradient is of the correct sign to be caused in part by gravity forces acting on the specimen in the testing machine. With the specimen deflected downward initially, axial stresses and bending stresses which accompanied straightening were additive on the upper edge of the plate. This effect however is believed to be small and nearly constant for the four specimens. The greater portion of the variation in edge stresses is attributed to lack of perfect alignment between the end tabs. Uneven distribution of load along the axis of either or both loading pins may be expected to affect the stress distribution on the bottom plate.

The loads carried by the bracket and bottom plate at Section A-A and the position of the computed load line for this section are given in Table 2. The sum of the computed bracket and bottom plate loads differed from the testing machine load of 160 kips. by −7.8 to 0.6%. The position of the calculated load line ranged from 7.41 to 8.91 in. above the center line of the bottom plate. Excluding specimen 1, the center line through the loading pins for

Table 2—Summary of Measurements and Results of Elastic Tests

Specimen	*1*	*2*	*3*	*4*
Testing machine load, P, kips.	160	160	160	160
Section A-A				
Area of section, in.[2]	21.74	21.75	21.69	21.54
Maximum bracket stress, ksi.*	12.0	17.3	14.2	15.8
Average bracket stress, ksi.	3.9	4.8	4.1	4.3
Bracket load, P_b, kips.	53.7	66.2	55.8	59.2
Location of resultant bracket load above lower edge of bracket, in.	2.83	2.29	2.76	2.55
Maximum bottom plate stress, ksi.	14.7	13.5	14.7	14.9
Average bottom plate stress, ksi.	11.8	12.0	12.2	12.8
Bottom plate load, P_b, kips.	93.9	94.7	96.6	98.2
Bottom plate load, $P - P_b$, kips.	106.3	93.8	104.2	100.8
Location of load line above center line of bottom plate, in.	7.41	8.91	7.68	8.07
Section B-B				
Area of section, in.[2]	20.26	20.45	19.82	19.79
Maximum longitudinal stress, ksi.	12.1	12.1	12.8	12.6
Average longitudinal stress, ksi.	8.4	7.7	9.0	8.4
Longitudinal load, kips.	103.8	96.6	106.7	100.5
Maximum bottom plate stress, ksi.	10.4	8.6	9.8	8.1
Average bottom plate stress, ksi.	5.6	7.6	6.9	7.0
Bottom plate load, kips.	44.5	60.2	54.4	54.5

* Stresses given are for the axial direction.

each specimen was 8.25 in. above the center line of the bottom plate. The differences between the position of the pin line and the position of the load line did not exceed 2% of the height of the specimens.

Figure 9 shows the relationship between the computed bracket and bottom plate loads and the position of the computed load line for section A-A. As is to be expected, it is observed that for load lines nearer the bracket, the bracket load increases and the bottom plate load decreases. It is probable that the bracket loads are more accurate than the bottom plate loads, since the stress distribution on the bracket is more easily defined by a limited number of stress values. Accordingly, it is also probable that bottom plate loads obtained by taking differences between the testing machine load and the computed bracket loads are more reliable than the computed bottom plate loads.

It is of interest to note in Figs. 7 and 9 that the bracket loads and maximum bracket stresses increase in the same specimen sequence. The correlation between the position of the load line and the stresses at the lower edge of the bracket and at the center of the bottom plate are discussed in the appendix. In view of the dependence of the measured stresses on the loads carried separately by the bracket and the bottom plate, together with the effects of misalignment of the end tabs, friction at the loading pins, and measurement errors, it is evident that variations between corresponding axial stresses for the different specimens should not be attributed entirely to design details.

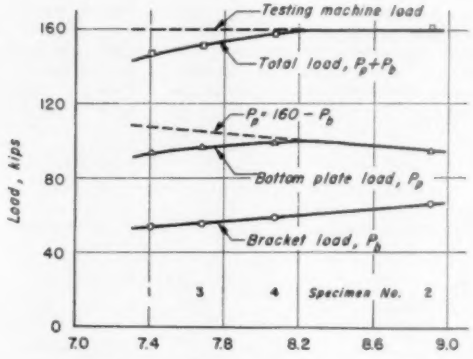


Fig. 9 (Right) Variation of calculated bracket and bottom plate loads with calculated position of resultant load line. Section A-A

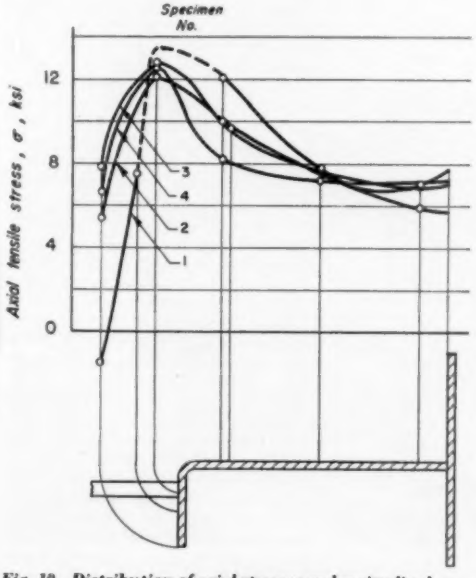


Fig. 10 Distribution of axial stresses on longitudinal, section B-B. Load 160 kips.

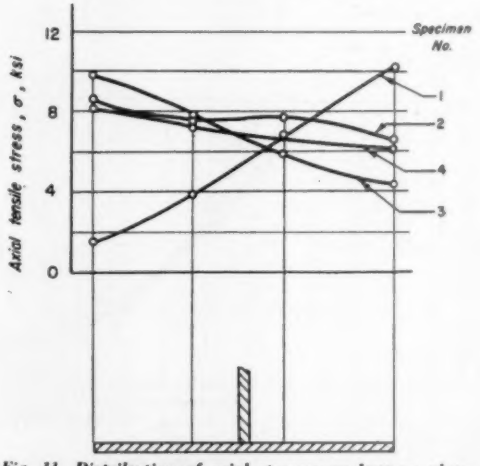


Fig. 11 Distribution of axial stresses on bottom plate, section B-B. Load 160 kips.

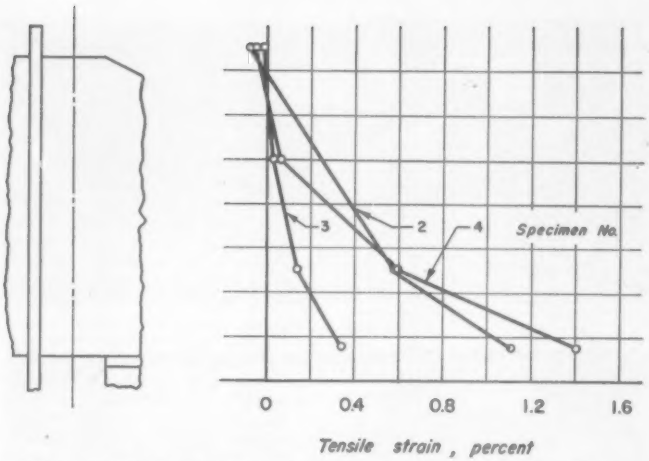


Fig. 12 Distribution of axial strains on bracket, section A-A. Load 600 kips

The axial stress distributions on the longitudinal and bottom plate of each specimen at section B-B are given in Figs. 10 and 11. Measured stresses on the longitudinal, Fig. 10, reached maximum values on the flange in line with the bracket. At this point stresses ranged from 1.5 to 1.7 times the average stress on the section. Stresses on the bottom plate at this section, Fig. 11, were distributed more or less linearly across the width of the plate, being slightly higher on the upper edge as was the case for section A-A. No marked change in stress magnitude was observed at the intersection of the longitudinal web and the bottom plate. Axial stress distributions on the longitudinal and bottom plate of Specimen 1 differed appreciably from those found for specimens 2, 3 and 4.

STRAIN DISTRIBUTION PRIOR TO FAILURE AT 0° F.

The axial strains for corresponding gage lines on opposite sides of the plate and in similar locations on the two ends of the specimen were averaged for each load, and the strain distribution on sections A-A and B-B of each specimen were plotted for comparison. Strain distributions for a load of 600 kips. are given in Figs. 12 to 15 for specimens 2, 3 and 4. Strain distribution for corresponding sections of the different specimens were similar and in general followed the stress distribution found in the elastic tests. Figures 12 to 15 show that strains extended into the plastic range at highly stressed points at this load. It was observed, however, that the strains at the lower edge of the bracket on Specimen 3 were exceptionally low compared with similar strains on specimens 2 and 4.

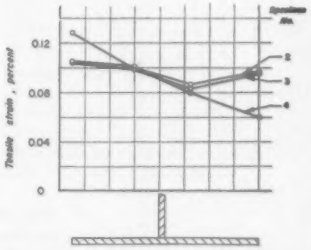


Fig. 15 Distribution of axial strains on bottom plate, section B-B. Load 600 kips.

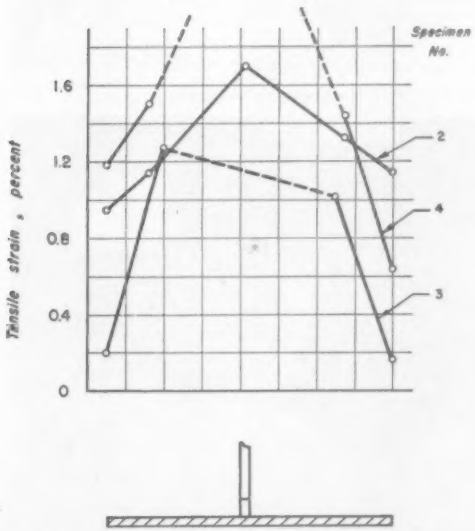


Fig. 13 Distribution of axial strains on bottom plate, section A-A. Load 600 kips.

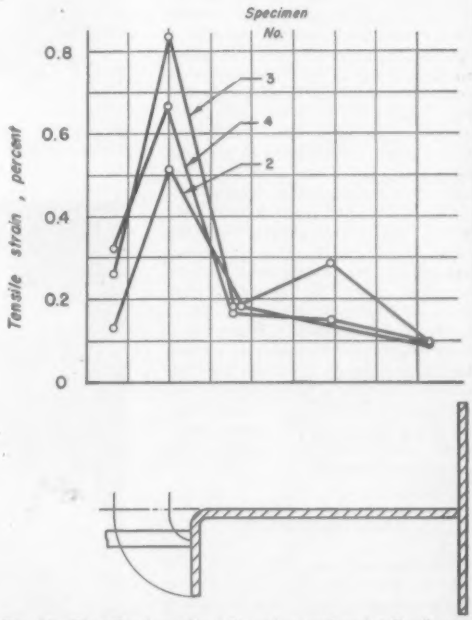


Fig. 14 Distribution of axial strains on longitudinal, section B-B. Load 600 kips.

Table 3—Result of Tests to Failure, 0° F.

Specimen	1	2	3	4
Energy to fracture, ft.-lb.	30,700	46,100	49,700	17,250
Maximum load, kips.	750	774	843	650
Average stress on longitudinal, section B-B, at failure	37.0	37.8	42.5	32.8

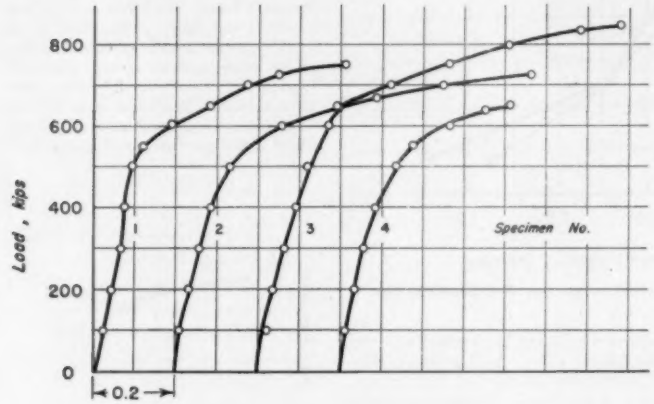


Fig. 16 Center line extension versus load

ENERGY TO FAILURE AT 0° F.

It was not possible to compare the energies required for failure of the different specimens for identical loading treatment. Specimens 1 and 2 failed initially near the end tabs and were subsequently rewelded for a second test. During the retest of Specimen 1, failure occurred in the testing machine fixtures. New fixtures were designed, and the test to failure of Specimen 1 constituted the third loading of this specimen. Specimen 2 was loaded to failure after the rewelding of the end. Specimens 3 and 4 were tested to failure on the first low temperature loading.

Curves of load vs. center line extension for the final low-temperature tests are shown in Fig. 16. The energy to failure was computed as the area under the load-extension curve, the extension being measured between the points E-E, Fig. 3. In the case of Specimen 2, which underwent considerable elongation on the first loading, the energy to failure was computed as the sum of the energies for the two tests minus the energy represented by the linear portion of the load-extension curve for the final test. Energy values are given in Table 3.

MAXIMUM LOADS

The maximum loads sustained by the four specimens are given in Table 3. Failures occurred by cleavage fractures of the bottom plate and bracket on one or both sides of the transverse bulkhead.

Figures 17 to 20 show the specimens after failure. After test, it was observed that all specimens were displaced laterally a small amount in the direction of the bracket. Since such displacement brings the bottom plate nearer the load line, it is believed that the bracket failures occurred first and were followed by failures in the bottom plate. Chevron patterns on the fracture surfaces indicated that fractures started at the underside of the bracket and at the center of the bottom plate where the stress concentrations were greatest. Fracture surfaces were coarse grained and generally perpendicular to the surface of the plate. A reduction in plate thickness of 7% was measured at the center of a dimple found at the center of the bottom plate fracture on Specimen 1. No other reduction in plate thickness at the edges of the fractures could be measured with micrometer calipers.

The average stress, P/A, on the longitudinal section B-B at failure was computed for each specimen and found to range from 32.8 to 42.5 kip./in.^{2}, Table 3. These values exceeded the tensile yield strength of the material (see Table 1) for all specimens except Specimen 4.

SUMMARY

The elastic stress distribution at room

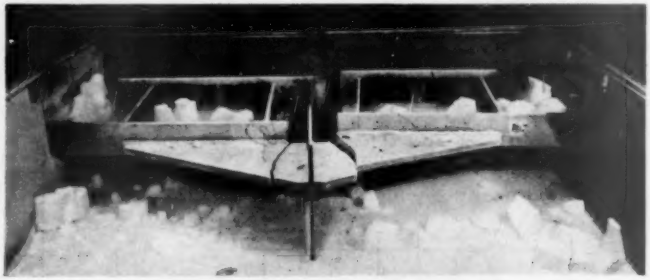


Fig. 17 Specimen 1 after test at 0° F.

Fig. 18 Specimen 2 after test at 0° F.

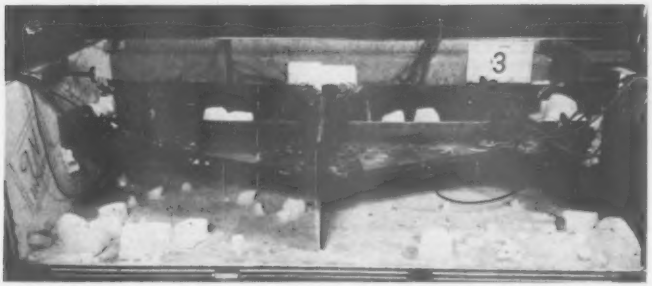


Fig. 19 Specimen 3 after test at 0° F.

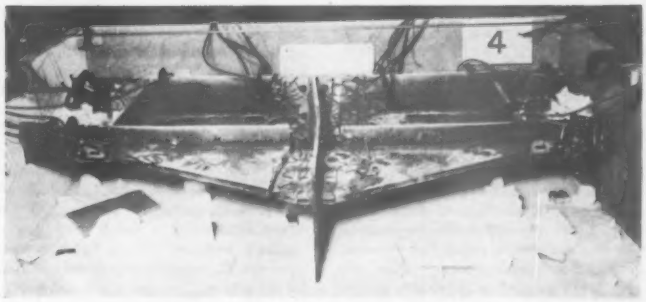


Fig. 20 Specimen 4 after test at 0° F.

temperature, strain distribution prior to failure at 0° F. and energy to failure at 0° F. were determined for four interrupted longitudinal tensile specimens, and the maximum loads were measured. Exceptionally high stresses were measured at the edge of semicircular cutouts in the longitudinals of two specimens. Stresses in similar locations on two specimens without cutouts were comparatively low. Stresses in the axial direction ranging from 1.5 to 2.2 times the average stress in the longitudinal were measured at the center of the bottom plate and at the lower edge of the bracket. The strain distributions for loads near failure did not differ appreciably from the elastic stress distribution. Three of the four specimens sustained considerable elongation before failure, but all specimens ultimately developed cleavage fractures in the area of the intersection.

APPENDIX

Load Line Calculations, Section A-A

The line of the resultant load on the specimen is calculated from the condition of moment equilibrium of the forces acting over any section *mn* Fig. 21. At section *mn* there exists an axis *z*, normal to the plane of the specimen, about which the moments of the bracket and bottom plate forces are equal. The load line *pq* must intersect this axis, since the moment of the external load is zero. The distance $\bar{y}$ of the load line above the center line of the bottom plate is obtained by equating the moment of the load *P* about point 0 to the moment of the bracket load P_b about the same point, so that

$$\bar{y} = \frac{M_b}{P} \qquad (1)$$

The moment M_b of the bracket load is computed from the axial stress distribution σ_a by summing the moments of forces acting on small elements of the bracket throughout its height *ab* in the *y*-direction. The moment Δm of such a force about point 0, Fig. 21, is

$$\Delta m_b = t \Delta y \, y \, \sigma_a \qquad (2)$$

and

$$M_b = t \Delta y \sum_{y=a}^{y=b} y \sigma_a \qquad (3)$$

Substituting Equation 3 in Equation 1,

$$\bar{y} = \frac{1}{P} t \Delta y \sum_{y=a}^{y=b} y \sigma_a$$

For purposes of calculation, Δy was taken as 0.4 in. Values of *y* were computed for each element, and values of σ_a at the centroid of each element were obtained from Fig. 7.

The position of the calculated load line was found to vary slightly for the different specimens (Table 2). It may be expected that this variation will alter the loads carried separately by the bracket and bottom plate on identical specimens. Figure 9 shows that the bracket load varied almost linearly with the calculated position of the load line on the four specimens tested. Since any change in the bracket and bottom plate loads must be accompanied by corresponding changes in stress, the relationships between changes in load line position and maximum bracket and bottom plate stresses are of interest. Figure 22 shows a plot of the maximum axial stress ratio for the bracket and bottom plate of each specimen (section A-A) versus the position of the computed load line. Comparison of Fig. 22 with Fig. 9 shows that although both the load and the peak stress ratio varied similarly for small

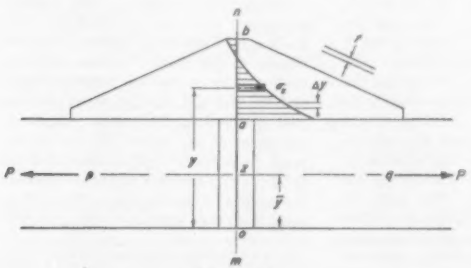


Figure 21

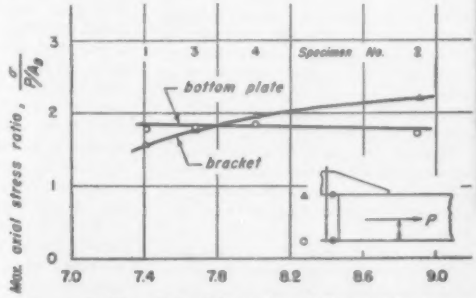


Fig. 22 Variation of stress ratio (concentration) at lower edge of bracket and at center of bottom plate with position of resultant load line. Section A-A

changes in the position of the load line, the stress ratio at the lower edge of the bracket is more sensitive to load line position than the stress ratio at the center of the bottom plate. This may be expected due to the high stress concentration and the relatively large part of the load carried by the lower portion of the bracket.

It is apparent however, that since the four specimens are structurally different, the measured stress variations may partially represent the effects of design details even though these variations are such as might be expected from differences in the position of the load lines.

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Discussion by Wendell P. Roop

In these tests a part of a welded structure is isolated from the rest in order to give it a laboratory test. In principle this is like any other separate test of a separate part of a mechanical assembly. One does not wait to start pumping oil through a thousand-mile pipe line to find out whether the type of gasket or weld used is strong and tight enough. At the same time, the only good detail is one that does well in the working structure. We want to give the structural elements of a ship the best possible start in life, even though their final test is that of service.

To foretell service behavior by laboratory test the gap between test and service conditions must somehow be closed. The conditions of service are never wholly matched in test. Many of the differences are simply ignored. Thus, in the present tests, a static load is used whereas in service the load on a ship is roughly cyclic, though with wide variations. In so far as this is significant, allowance must be made for it by inference, since what happens in service differs from what happens in test. This difference in time pattern of load is not important in connection with casualty by grounding, collision or explosion, but might be so in connection with casualty by fatigue.

Matching of test with service refers to equivalence in two respects,* external and internal. In the present tests internal equivalence is well achieved: the test structures were based on full-scale working plans, and the models were fabricated at full scale in a shipyard. More data on the material would be useful; although it is ship steel, such material needs better identification. Its behavior in the Navy tear test should be known.

Comment is now offered with respect to the external equivalence of the test model with the corresponding segment of a complete structure under designed load. The time pattern of the load has been mentioned; what remains is the space pattern. This refers to the boundaries along which the model is cut out of the larger structure, and to the test loads applied at those boundaries.

The problem is to choose a significant element of construction and to place its boundaries so as to meet the condition that test loads applied to these boundaries shall simulate those at the corresponding surfaces in the completed structure. This simulation will never be complete, but it must come close enough, and the test must give data enough, so that the gap between test and service can be closed.

The uncertainties are of three sorts. First, the service load on the whole structure has a complex pattern of bending, shear, torsion, local pressure. This pattern varies in time with respect to the proportions of these elements of load. No attempt is made to combine them in test; rather it is assumed that when action under each element of load is known they may be superposed. The present tests considered only direct tension of the sort which would be caused by pure bending of the whole ship.

Secondly, the peak intensities of load in service are unknown. Design is based on nominal values. The test is concerned, at most, with these nominal values.

Thirdly, in principle, load might act on the element simulated by the model through any of the boundaries by which the element is cut from the larger structure. A complete model test would require control of load on all such boundaries. A compromise is made by placing some of these boundaries in such a way that the load which would act through them if the rest of the structure were not absent but present could be ignored.

This third uncertainty is the point on which ingenuity in a test of this sort can be used with most benefit. In the present tests the choices which had to be made referred to the width of shell plating and to the centering of the applied tension. The fact that the chosen width of plating did not agree with a certain rule used in design was not important. Even the change in thickness of shell plating was not important because the load was centered on the actual model section and not on the simulated ship section. The only eccentricity of load on the model as a whole was that caused by changes in geometry during the test. The bracket by itself was eccentric from the start.

In the whole ship, in pure bending as assumed, the shear on longitudinal cuts like the shell plate boundaries would be in fact zero. The normal component of load on these boundaries is zero in the model but need not be zero in the ship. However on the cuts as made in the present model it is safe to ignore them. In this respect these tests were better planned than those earlier ones* which formed almost their only precedent. In that case the cut was made through the middle of the element, namely, a hatch, with the intent of isolating a hatch corner as a separate element. On a hatch corner, however, the transverse loads are essential to the action. The isolated hatch corner is, in effect, a hook, but to simulate its action in the ship it would have to be prevented from simply pulling out under load as an open hook does. This could be done by matching two hooks with each other in parallel, or, in the hatch, by including both off the corners at one end. The distance separating them, the width of the opening, is not critical. One corner alone, however, fails completely to simulate the action in the whole hatch.

In the present tests a similar situation occurs in the split of the tensile load between the bracket and the shell, but instead of trying to test the eccentric bracket as a separate element, the two elements are combined in the model as in the ship. The rosette data in Fig. 6 reveal the presence of components of load in the transverse direction, especially in those filleted designs which are said, from the hatch corner tests, to be most favorable.

The rosette data were taken at a nominal stress of 7.27 ksi. At 600 kips. load, 27.27 ksi. nominal stress, strains of the order of 1% occurred. The author's statement that "strain distribution for loads near failure did not differ . . . from the elastic . . . distribution" seems not to be confirmed by comparison between Figs. 7 and 12, or 8 and 13. Since the load was well centered in the elastic range, the variations at the bracket heel in Fig. 12 result from plastic readjustment, and this is the most significant difference between the models.† Figure 12 makes Model 3 the most favorable, but before that design is

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* "Tests of Ductility in Ship Structure," by W. P. Roop; *A.S.T.M.*, STP No. 87, 1949.

* "Tests of Various Designs of Welded Hatch Corners," by E. P. de Garmo; *Welding Research Council*, XIII, pp. 50–68, 1948.

adopted the reason for this result should be more clearly known.

A possible explanation is suggested as follows. The bracket, being strongly eccentric, tends to move toward the load line. In models 2 and 4 this is resisted only by the thin bulkhead but in Model 3 the two tripping brackets offer powerful support. The bracket is thus held up to its load and resists it more strongly. There is no evidence, however, that the bearing plates are needed.

However, this is not the whole story since the plastic readjustment also affects longitudinal extensions. Figure 16 shows that Model 3 is stiffer also in this respect. Consider the following table of over-all extensions at load 600 kips. The gage length is about 140 in.

Model	2	3	4	Elastic uncut longitudinal
Extension in.	0.255	0.170	0.270	0.133

These numbers suggest that models 2 and 4 might overload the shell plating at the bulkhead and in Figs. 6 and 13 it appears that something of the sort does in fact occur.

Although the emphasis in these tests is mainly on elastic and the earliest phases of plastic action yet they were carried through to fracture and in that connection give important information. First of all, as in other experiments,* brittle fracture is preceded by sizable deformation and, polocally, by rather high strain. Extrapolating to the lower edge of the bracket in Fig. 12 would bring us to strain values which might rise toward 10% before fracture.

Figure 16 shows that the extension beyond load 600 kips. in Model 4 was only 0.14 in.; but if this occurred mainly on a gage length of about 4 in. at the heel of the bracket it would add a strain there of 3.5%. In Model 3, however, extension after 600 kips. load was 0.7 in. While this was spread more uniformly than in Model 4, yet a local rise to 10% strain before fracture is not at all excluded. Ten per cent may be taken as a rough value of the local ductility for this steel at this temperature.

In summary, the merit of Model 3 over Model 4 lies not only in its greater ultimate strength, but especially in its greater capacity for deformation before fracture, even though that fracture is in brittle mode. This may be ascribed to the smaller localization of plastic strain obtained by the support given to the bracket at its heel, holding it up to its eccentric load.

The tapering ends of the longitudinals in models 2 and 4 would serve to ease hard spots if the axial strains were the only important ones. The data show that these tapers are ineffective. It is concluded that the transverse actions predominate in determining the behavior of this structural element.

* "Tests of Large Welded Steel Box Girders," by A. H. Stang and B. S. Jaffe; Welding Research Council, XIV, pp. 89–97, 1949.

Stress Studies of Welded Ship Structure Specimens

Discussion by Comdr. James McIntosh, USCG and James B. Robertson, Jr.

The author is to be congratulated on this excellent paper.

The purpose of this discussion is to amplify the background, purpose and probable future direction of the investigation covered by this paper.

As has been stated, the investigation was initiated for the purpose of studying certain details of welded ship construction which had proved troublesome. Thus, specimen No. 1 represents the original design of the intersection of a bottom longitudinal with a transverse bulkhead in a Maritime Commission Type T2 tank vessel, a detail which had been involved in numerous structural failures. Specimens 2, 3 and 4 represent modifications of the original detail made with a view toward ameliorating the difficulties which had been encountered.

While the basic detail, as installed in a vessel, is subjected to a rather complex system of forces including direct tension, compression and shear due to bending of the hull, normal forces produced by water pressure on the ship's bottom which are partly balanced by forces due to the weight of the cargo carried in the hull, and a load carried by and in the plane of the transverse bulkhead, it was decided that a comparative evaluation of the four designs could be made on the basis of their performance under direct tensile load. It was thought that any attempt to duplicate actual service loads would so complicate the testing as to make it virtually impracticable, and would introduce many variables which would complicate interpretation of results.

It is to be noted that the specimens as constructed and tested deviate from the scantlings actually employed in the T2 tankers in that the strip of bottom plating was $^1/_2$ in. in thickness in place of the $^9/_{16}$-in. plating of the T2 bottoms. Also, the width of the strip of bottom plating is not equivalent to that generally assumed to be effective in conjunction with a given stiffening member. This lightening of the bottom plating was undertaken in order to insure that the specimens would be tested to destruction in the 1150 kip machine. It is the opinion of the discussors that this lightening of the bottom plating adversely affected the performance of the "softer" specimens tested:

> As the specimens were pulled, the plastic elongations of the strips of thinner bottom plating were proportionately greater than the elongations of the brackets. Thus the specimens tended to bend as if the brackets were hinges with the resultant failures being initiated in the thicker, more brittle material of the brackets. It will be noted that with each specimen the crack in the bracket passes either through or very close to the point of juncture between the bracket and the longitudinal. This would explain the relatively better performance of the specimens without

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The paper by W. R. Campbell is published in this issue of the Research Supplement.

cutouts in that the material retained in the longitudinal, together in one case with the doubler added to the bottom plating, was effective in resisting the disproportionate elongation of the bottom plating.

It is believed that the effect described above was more pronounced in the specimens tested than would be actually found in an actual installation in a vessel. Inasmuch as the maximum breaking loads employed in testing the four specimens described in the paper indicate that it may be possible to test more nearly full-size specimens, it is proposed to repeat the series with such specimens. It is believed that a more valid evaluation of the details would be obtained from this second series.

The opinions expressed herein are not necessarily those of the U. S. Coast Guard or the Ship Structure Committee.

Reports of International Welding Commissions

The various Commissions of the International Institute of Welding met during the week 5-10 June last in Paris, and their Chairmen submitted verbal reports of their proceedings in a plenary session there. We print below a selection from these reports, which, it is thought, will be of general interest to our readers

Commissions Nos. 10 and 11—Residual Stresses and Stress Relieving

Chairman: Dr. R. WECK

Stress Relieving

The Commission completed an enquiry concerning requirements for heat treatment in different countries. The results of the enquiry will be made available in the form of a table. No research work on stress relieving has been carried out in any country during the past year, but definite proposals for such work have been formulated in Belgium concerning the effectiveness of local stress relieving by heating in sections. France will consider an investigation of the problem of local stress relieving by progressive heating. From information submitted by Dr. Forsman of Sweden, where the method of stress relieving by plastic deformation is in current use, it appears that satisfactory results have been obtained with this method over a number of years. Peening as a method of stress relieving is extensively used in the United States with satisfactory results.

Measurement of Residual Stresses

The proposal to issue a document on the measurement of residual stresses could not be brought to conclusion because the information in the possession of the Commission is not complete. A number of reports which bear on this question have, however, been submitted by Mr. Gunnert (Sweden), Prof. Soete (Belgium), Mr. Milbradt (United States) and Dr. Finn Jonassen (United States). Further information will be forthcoming from Belgium, Great Britain and the United States.

Comparison of Methods of Measurement

This work is proceeding. Six countries are participating and the test plates have been made by Great Britain and have been despatched. It is expected that the results will be available for consideration at the next meeting of the Commission.

Brittle Fracture

In a joint session with Commission No. 12 the problem of the effect of residual stresses in producing brittle fracture was discussed on the basis of a report submitted by M. Louis of Belgium, concerning brittle failures in welded bridges. Although there was considerable difference of opinion on details, the general conclusion of the meeting was that residual stresses can contribute to brittle fracture under certain circumstances, and that it is, therefore, necessary for this aspect of the problem to be further investigated. Work, with this object in view, is in progress in Great Britain.

Effects of Residual Stresses on Fatigue

The effect of residual stresses on fatigue was discussed on the basis of two reports submitted by Prof. Ros of Switzerland and Prof. Soete of Belgium. Although the work described in these reports differed considerably in conception and execution, the same conclusions can be drawn from both investigations. Under the conditions realized in these investigations, which did not in any way inhibit plastic deformation in the test pieces used, residual stresses have been found to decrease the fatigue strength for the one million and two million cycle limits by approximately 10 per cent. In test pieces containing quite normal

(*Continued on page 90-s*)

Some Metallurgical Aspects of Ship Steel Quality

A procedure has been developed for studying the influence of chemical composition and deoxidation upon the transition temperature of ship steels. Transition temperature is progressively raised by increasing the carbon, phosphorous, and vanadium contents

by H. M. Banta, R. H. Frazier and C. H. Lorig

INTRODUCTION

EXTENSIVE research, conducted under the auspices of the Ship Structure Committee, has revealed that the transition temperature of the plate steel, the temperature at which the steel changes from ductile to brittle behavior, is associated with the service performance of the steel in welded ship structures. Subsequent studies have indicated that steels which were made under conditions normally considered identical, and processed in the same manner, can differ widely in notched-bar properties, although their chemical composition and tensile properties are similar. Further consideration of this problem revealed the need for research to determine the causes for these differences in behavior. It appears that, by first determining and then by controlling the pertinent factors influencing the transition-temperature characteristics, ship plate could be produced with improved properties.

With the above objects in view, the Bureau of Ships on behalf of the Ship Structure Committee and the Committee on Ship Steel of the National Research Council established a research project, Contract NObs 50020, for the purpose of studying the influence of chemical composition and deoxidation upon the transition characteristics and tensile properties of American Bureau of Shipping Class A and B steels. All plates prepared on this project were $^3/_4$ in. in thickness; this thickness is, however, not characteristic of commercial Grade A steel.

REPRODUCIBILITY OF LABORATORY STEELS

Since it was proposed to conduct most of this investigation in the laboratory, using small ingots poured from induction-furnace melts, it was first necessary to determine whether or not semikilled laboratory heats could be produced having uniform tensile properties and transition-temperature characteristics from heat to heat. To answer this question, a series of five Class A and five Class B steels were made and tested.

This series of steels was prepared from 200-lb. induction-furnace melts, the charge was melted under an atmosphere of argon to insure low, uniform nitrogen content of the same order found in commercial ship plate. After the charge was melted and the desired temperature was obtained, the melt was partly deoxidized by an addition of silicomanganese to insure consistent recovery of the subsequent ferromanganese and ferrosilicon additions. Carbon, in the form of graphite, was added just prior to tapping to meet the desired specification. The entire heat was poured directly into a 6 x 6-in. big-end-up mold and the ingot capped with a steel plate when necessary.

The ingots were processed by heating to 2250° F., followed by forging to slabs $1^1/_4$ in. thick by 6 in. wide. After reheating to 2250° F., the slabs were rolled to $^7/_8$-in. gage, using reductions of approximately $^1/_4$ in. per pass. In order to insure a uniform finishing temperature, the $^7/_8$-in. sections were immediately recharged in a furnace held at 1650° F. After 20 min. or more in the 1650° F. furnace, the plates were reduced to $^3/_4$ in. in one pass. Following this final pass, the plates were placed on edge on a brick floor, with a brick separating each plate, and allowed to air cool.

Drillings for chemical analysis were taken from the top and bottom of each ingot following rolling. These analyses are shown in Table 1. The carbon contents of the Class A heats are in the range of 0.19 to 0.26% with eight of the ten values falling between 0.20 and 0.24%. The manganese contents of these same heats all fall within the range of 0.44 to 0.52%. The phosphorus, silicon and nitrogen contents are comparable with commercial steels of this grade.

The carbon content of all the Class B steels falls between 0.16 and 0.18%, with the exception of one value for the bottom of one ingot which is 0.20%. The

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Presented at Thirty-first Annual Meeting, A.W.S., Chicago, Ill., week of Oct. 22, 1950.

The opinions and statements made in this paper are those of the authors and are not to be construed necessarily as reflecting the views of the Navy Department or the Ship Structure Committee.

Table 1—Chemical Analysis of Class A and Class B Steels Made to Study the Reproducibility of Laboratory Steels. Finishing Temperature—1650° F.

Identification of steel			Chemical analysis, %					
Grade of steel	Heat No.	Location of sample	C	Mn	P	S	Si	N
Class A	A-1	Top	0.21	0.52	0.009	0.026	0.04	0.004
	A-1	Bottom	0.21	0.52	0.008	0.028	0.03	0.004
Class A	A-2	Top	0.23	0.47	0.021	0.029	0.08	0.004
	A-2	Bottom	0.26	0.47	0.017	0.030	0.06	0.003
Class A	A-3	Top	0.23	0.48	0.013	0.029	0.04	0.003
	A-3	Bottom	0.24	0.46	0.013	0.032	0.04	0.003
Class A	A-4	Top	0.20	0.44	0.012	0.025	0.04	0.003
	A-4	Bottom	0.23	0.45	0.014	0.029	0.03	0.003
Class A	A-5	Top	0.19	0.49	0.013	0.024	0.04	0.004
	A-5	Bottom	0.23	0.48	0.014	0.030	0.03	0.003
Class B	B-2	Top	0.16	0.87	0.017	0.028	0.03	0.004
	B-2	Bottom	0.17	0.88	0.014	0.028	0.04	0.002
Class B	B-3	Top	0.17	0.93	0.020	0.028	0.03	0.005
	B-3	Bottom	0.18	0.93	0.019	0.028	0.04	0.003
Class B	B-4	Top	0.18	0.91	0.018	0.029	0.03	0.005
	B-4	Bottom	0.18	0.89	0.017	0.029	0.02	0.004
Class B	B-5	Top	0.18	0.99	0.015	0.029	0.07	0.004
	B-5	Bottom	0.18	1.03	0.019	0.029	0.09	0.004
Class B	B-6	Top	0.17	0.92	0.014	0.025	0.06	0.006
	B-6	Bottom	0.20	0.89	0.015	0.029	0.04	0.003

manganese content of four of these heats is between 0.87 and 0.93%, while the fifth heat is higher, the top and bottom being 0.99 and 1.03%, respectively. The other constituents are comparable with those of the Class A steels.

Duplicate standard plate tensile specimens, using the full thickness of the plate, were prepared from each heat. The data from these specimens, together with tear test data and the transition temperature determined from keyhole Charpy specimens, are shown in Table 2.

It will be noted that the tensile properties of the Class A steels are extremely uniform with the ultimate strength falling between 58,900 and 61,600 psi., and the yield strength between 37,050 and 38,800 psi.

The tensile strength of the Class B steels falls between 57,900 and 63,900 psi., and the yield strength between 36,200 and 42,800 psi. The ductility, as indicated by the elongation, is essentially the same for both classes of steel and in the range expected for this type of steel.

The transition temperature of these steels was determined by two methods: first, by using the Navy tear test, and second, from notched-bar impact data obtained from keyhole Charpy specimens.

The tear tests were made using the type of specimen and procedure as described by Kahn and Imbembo,[1] with the exception that, in rating the appearance of the specimen, only the first $1^1/_2$ inches of the fracture was considered, since the last $^1/_2$ in. tends to be of the cleavage type, even when the remainder is quite ductile. In no case, however, did this change in procedure alter the results of transition temperature determinations.

The tear test specimen, which is 3 x 5 in. long, the latter dimension being in the direction of rolling, utilizes the full thickness of the plate. This test specimen is shown in Fig. 1. The specimen is subjected to tensile loading through a pin and shackle arrangement while submerged in a liquid bath for temperature control. The transition temperature was defined as the highest temperature at which one or more specimens exhibited a fractured area of less than 50% of the ductile shear type.

From Table 2 it will be noted that the tear test transition temperature of the Class A steels is quite uniform, four of the steels falling between 50 and 55° F., and one at 45° F. In the case of the Class B steels, four have tear test transition temperatures of 40° F. The fifth steel, Heat B-6, however, is entirely out of line, having a transition temperature of 0° F. Subsequent examination of this heat revealed that the grain size was appreciably finer, as compared with the other

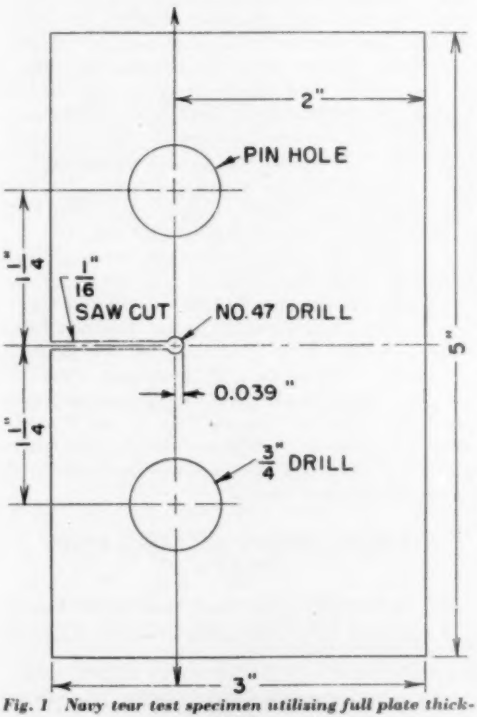

Fig. 1 Navy tear test specimen utilizing full plate thickness

Table 2—Tensile and Transition-Temperature Characteristics of Class A and Class B Steels in Reproducibility Study of Laboratory Steels Finished at 1650° F.

		Tensile properties			Tear test properties				
Grade of steel	*Heat No.*	*Yield strength, psi.*	*Tensile strength, psi.*	*Elongation in 8 in., %*	*Maximum load, lb.*	*Energy start, ft-lb.*[a]	*Energy to propagate, ft-lb.*[a]	*Transition temperature, ° F*	*Charpy impact*[b] *transition temperature, ° F.*
Class A	A-1	37,050	61,600	21.5*	38,640	840	790	50	−2
Class A	A-2	38,800	60,600	32.5	38,160	820	840	45	−16
Class A	A-3	37,700	59,500	30.0	37,150	690	680	55	+16
Class A	A-4	37,650	58,900	33.5	37,410	730	630	50	0
Class A	A-5	38,600	59,100	29.0	37,660	810	640	50	+10
Class B	B-2	35,700	57,900	31.0	38,670	910	680	40	−16
Class B	B-3	36,200	61,500	31.0	40,630	960	700	40	−32
Class B	B-4	38,300	59,900	30.5	40,260	990	710	40	−25
Class B	B-5	40,900	63,900	30.5	43,790	950	660	40	−34
Class B	B-6	42,800	63,200	32.5	42,270	920	820	0	−38

* NOTE: Poor surface quality: (*a*) Average energy of the four ductile specimens broken 10° F. above the transition temperature and (*b*) Temperature at which the impact strength is 20 ft-lb.

heats in this series. A careful study of the melting, forging and rolling records failed to satisfactorily explain this difference.

The transition temperature was determined from the keyhole Charpy impact data by arbitrarily taking the temperature at the point on the temperature-Charpy impact curve where it crossed the 20-ft.-lb. level. In using this procedure, it was realized that it is open to question; however, no altogether satisfactory procedure has been agreed upon by those working in this field. A typical example of the Charpy impact data is shown in Fig. 2. All Charpy specimens were taken with the long axis in the direction of rolling and were notched in the direction perpendicular to the original surface of the plate.

From Table 2, it will be observed that the Charpy impact transition temperatures of the Class A steels all fall between −2° F. and +16° F., with the exception of Heat A-2 which was −16° F. This heat also had the lowest transition temperature of the group, as determined by the tear test.

The Charpy impact transition temperatures of the Class B steels fall between −16° F. and −38° F. Heat B-6, which has the lowest Charpy transition temperature, also had the lowest tear test transition temperature. The Charpy test, however, in this case, did not indicate the marked difference between Heat B-6 and the other heats in this group as the tear test did.

From the above review of the data in Table 2, it appears that 200-lb. semikilled laboratory heats can be produced with sufficient reproducibility to justify their use in studying the influence of chemical composition and deoxidation upon the characteristics of ship plate

INFLUENCE OF FINISHING TEMPERATURE AND GRAIN SIZE

Since the fine grain size of Heat B-6, as compared with the other heats in this series, B-2 to B-5, inclusive, appeared to have a marked influence upon the transition temperature as determined by the tear test, it was decided to further investigate this subject. In order to eliminate the possibilities of variables other than grain size, as established by the finishing temperature, a heat of Class A steel was forged to a $1^1/_4$-in. slab and then divided into three parts. These three parts were then processed to $^3/_4$-in. plate, as previously described, with the exception that each piece was heated to a different temperature prior to the final pass through the rolling mill, these temperatures being 1650, 1750, and 1850° F. The object was to vary the grain size by changing the

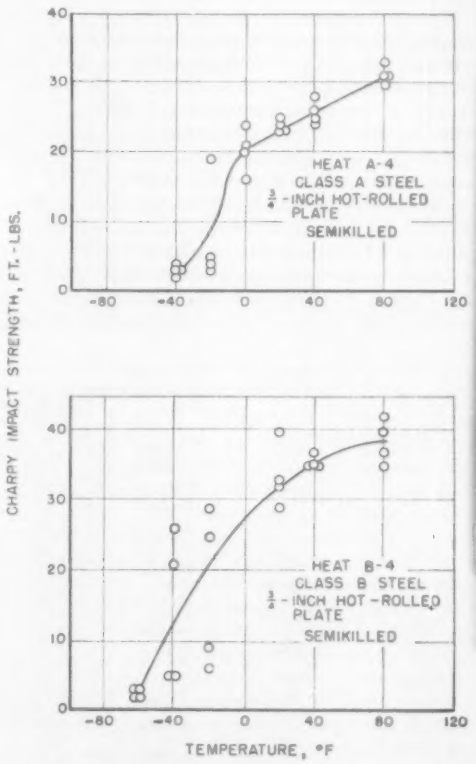

Fig. 2 Typical keyhole charpy impact data for laboratory class A and B steels finished at 1650° F.

Table 3—Chemical Analysis and Properties of Steels Made to Study the Influence of Finishing Temperature and Grain Size

Grade of steel	Heat No.	Composition, % C	Mn	P	S	Si	N
A	A6424	0.21	0.42	0.014	0.029	0.02	0.004
B	A6365	0.19	0.95	0.021	0.028	0.06	0.004

Grade of steel	Heat No.	Finishing temperature, ° F.	Yield strength, psi.	Tensile strength, psi.	Yield strength/ tensile strength, ratio	Elongation in 8 in., %	Tear test transition temperature, ° F.
A	A6424	1650	37,950	57,200	66.3	31.5	+50
A	A6424	1750	34,650	57,600	60.1	31.0	+60
A	A6424	1850	34,000	58,250	58.4	29.0	+95
B	A6365	1650	40,150	64,000	62.8	29.0	+10
B	A6365	1750	38,650	63,700	60.7	32.0	+20
B	A6365	1850	38,150	63,850	59.8	31.5	+40

finishing temperature. A heat of Class B steel was processed in a similar manner.

The chemical analyses of these two steels, together with other pertinent data, are shown in Table 3. It will be noted that there is a fair correlation between the finishing temperature and grain size distribution as shown by the photomicrographs in Fig. 3.

As would be expected, the finishing temperature did, in the case of both grades of steel, influence the yield strength-tensile strength ratio, this ratio decreasing slightly with increased finishing temperature. This effect was most noticeable in the Class A steel.

The most pronounced effect of varying the finishing temperature was noted in the transition temperature, which increased as the finishing temperature was raised. Of the two steels, the Class A appeared to be the most sensitive; increasing the finishing temperature from 1650 to 1850° F. raised the transition temperature 45° F. Under similar conditions, the transition temperature of the Class B steel was raised 30° F.

These results indicate that the transition temperature is influenced to a marked extent by the finishing temperature. It also appears that the Class A steel is more sensitive to changes in the finishing temperature than the Class B steel, but additional data are required to confirm this point. A comparison of the data in Table 3, with results from commercial steels of these two grades, showed that the higher finishing temperature, 1850° F., produced properties most similar to the commercial product. It was decided, therefore, to use the 1850° F. finishing temperature in the remainder of this investigation, provided consistent properties could be obtained.

REPRODUCIBILITY OF LABORATORY STEELS FINISHED AT 1850° F.

Prior to continuing this investigation, it was considered desirable to repeat the series previously made to determine the reproducibility of laboratory steels, using the same melting and processing procedure as previously described, with the exception that the finishing temperature was raised from 1650 to 1850° F. and a slightly heavier reduction was taken in the final pass. The slabs were reduced from $^9/_{10}$ to $^3/_4$ in. in the last pass following reheating to 1850° F.

The chemical analysis of this second series of five Class A and five Class B steels is listed in Table 4, analyses being shown for the top and bottom of each ingot following rolling to $^3/_4$-in. plate.

In the Class A steels, the carbon content in four of

Table 4—Chemical Analysis of Class A and Class B Steels Made to Study the Reproducibility of Laboratory Steels. Finishing Temperature—1850° F.

Identification of steel: Grade of steel	Heat No.	Location of sample	Chemical analysis: Composition, % C	Mn	P	S	Si	N
Class A	A6555	Top	0.22	0.47	0.016	0.025	0.07	0.004
		Bottom	0.22	0.48	0.017	0.024	0.06	0.003
Class A	A6556	Top	0.23	0.45	0.017	0.025	0.06	0.004
		Bottom	0.23	0.44	0.018	0.025	0.05	0.005
Class A	A6587	Top	0.20	0.45	0.011	0.023	0.07	0.005
		Bottom	0.25	0.45	0.011	0.025	0.06	0.003
Class A	A6650	Top	0.22	0.45	0.012	0.024	0.04	0.005
		Bottom	0.22	0.48	0.012	0.022	0.04	0.003
Class A	A6705	Top	0.22	0.49	0.016	0.025	0.05	0.004
		Bottom	0.21	0.49	0.017	0.025	0.06	0.004
Class B	A6557	Top	0.22	0.76	0.017	0.025	0.07	0.006
		Bottom	0.22	0.75	0.016	0.025	0.07	0.004
Class B	A6584	Top	0.20	0.78	0.014	0.021	0.07	0.005
		Bottom	0.21	0.75	0.014	0.023	0.07	0.004
Class B	A6588	Top	0.23	0.80	0.011	0.024	0.07	0.005
		Bottom	0.20	0.79	0.012	0.024	0.06	0.004
Class B	A6641	Top	0.20	0.79	0.016	0.023	0.04	0.004
		Bottom	0.19	0.83	0.016	0.022	0.04	0.004
Class B	A6651	Top	0.19	0.75	0.017	0.024	0.01	0.006
		Bottom	0.19	0.74	0.017	0.023	0.01	0.005

the heats is between 0.21 and 0.23%, and the manganese in all five heats is within the limits of 0.44 and 0.49%. The carbon and manganese contents of the Class B steels are between 0.19 and 0.23% and 0.74 to 0.83% respectively. The remaining elements, phosphorus, sulphur, silicon and nitrogen, all fall within relatively narrow limits and are present to about the same extent as in commercial steels.

The tensile properties of these steels together with the transition temperatures, as determined by both the tear test and from keyhole Charpy impact data, are listed in Table 5. The tensile properties of both classes are fairly uniform and in the range expected for steels of this composition.

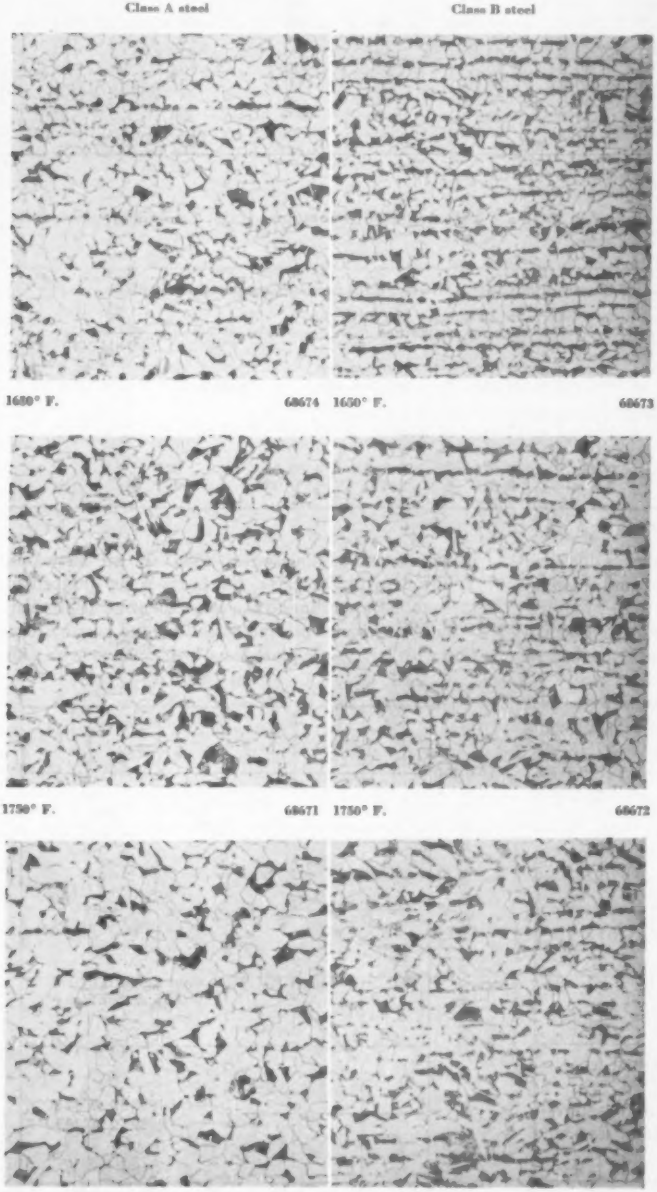

Fig. 3 Microstructure of class A and class B steels finished at 1650, 1750 and 1850° F. 100 ×

The tear test transition temperatures of the Class A steels are all between 60 and 80° F. with one exception: Heat A6587 which has a transition temperature of 100° F. The data from the keyhole Charpy specimens did not show this difference in behavior of Heat A6587 as indicated by the tear test. Using the Charpy impact data, the transition temperatures of these Class A steels range from 4 to 25° F.

Of the five Class B steels, four have a tear test transition temperature of 70 and one 80° F. The keyhole Charpy data indicate transition temperatures from −6 to −24° F.

From the above data, it may be safely concluded that 200-lb. semi-killed ingots of both Class A and B steels can be made in the laboratory with sufficient reproducibility that they can be used for investigating the influence of chemical composition and processing upon steel quality.

INFLUENCE OF CARBON, MANGANESE, PHOSPHORUS, SULPHUR, SILICON AND VANADIUM

The influence of chemical composition in both Class A and B steels was studied by varying the carbon, manganese, phosphorus, sulphur, silicon and vanadium contents within the limits shown in Table 6. Only one constituent in each heat was varied from the nominal composition as listed in Table 6, so that the effect could be readily determined.

Also, in order to continue the investigation of the effect of finishing temperature, while studying the influence of chemical composition, the first series of eight heats made to determine the effect of carbon and manganese contents was finished at

Table 5—Tensile and Transition-Temperature Characteristics of Class A and Class B Steels Made to Study the Reproducibility of Laboratory Steels. Finishing Temperature—1850° F.

Identification of steel		Tensile properties				Tear test properties				
Grade of steel	Heat No.	Yield strength, psi. Upper†	Yield strength, psi. Lower‡	Tensile strength, psi.	Elongation in 8 in., %	Maximum load, lb.	Energy[a] to start, ft.-lb.	Energy[a] to propagate, ft.-lb.	Transition temperature, ° F	Charpy impact[b] transition temperature, ° F
Class A	A6555	38,850	36,050	62,700	27.5	37,920	840	750	80	+12
Class A	A6556	37,950	35,000	61,600	31.0	38,230	790	690	70	+4
Class A	A6587	35,400	34,450	61,650	29.5	35,920	690	680	100	+12
Class A	A6650	35,600	34,250	60,550	28.0	36,740	820	640	70	+25
Class A	A6705	37,050	35,900	63,000	24.5	36,340	760	580	60	+5
Class B	A6557	36,200	35,500	61,700	30.0	40,200	960	910	70	−13
Class B	A6584	36,350	35,400	61,950	30.5	39,450	850	700	70	−6
Class B	A6588	35,550	34,900	62,350	28.0	38,760	760	780	70	−20
Class B	A6641	36,550	35,350	62,850	24.0	39,910	890	660	80	−25
Class B	A6651	37,200	35,700	62,300	28.5	38,970	880	726	70	−24

† Upper yield strength—the maximum strength before "drop of the beam."
‡ Lower yield strength—lowest strength during "drop of the beam."

(*a*) Average energy of the four ductile specimens broken 10° F above the transition temperature and (*b*) Temperature at which the impact strength is 20 ft-lb.

Table 6—Nominal Composition of Class A and Class B Steels and Range of Composition Studied

Range of composition studied in class A and B steels

Grade of steel	Chemical composition, % C	Mn	P	S	Si	V
Class A	0.12/0.37	0.26/1.31	0.009/0.058	0.022/0.046	0.03/0.31	0.06/0.20
Class B	0.14/0.35	0.22/1.48	0.015/0.054	0.021/0.046	0.02/0.29	0.08/0.20

Nominal composition of class A and B steels

Grade of steel	Chemical composition, % C	Mn	P	S	Si	N
Class A	0.25	0.45	0.015	0.030	0.05	0.004
Class B	0.19	0.85	0.015	0.030	0.05	0.004

1650° F., while the remainder, including additional heats for studying the effect of carbon and manganese, was finished at 1850° F.

The chemical analyses, tensile properties and transition temperatures of the first eight heats, finished at 1650° F., are listed in Table 7.

Similar data for the fifteen Class A and fifteen Class B heats made to study the effect of the six constituents listed above and finished at 1850° F. are shown in Tables 8 to 11, inclusive, the heats being grouped in these tables according to the constituent being studied.

Since previous work indicates that the best correlation with large-scale plate tests,[2] which have been gen-

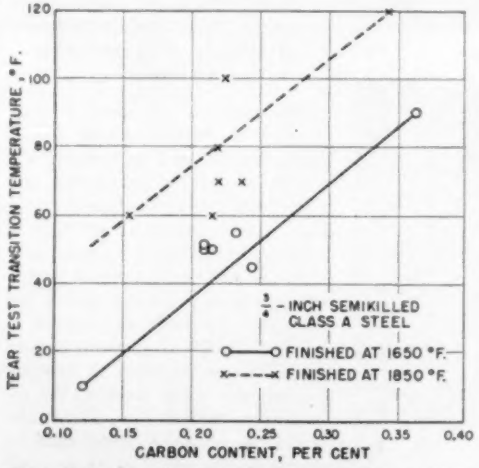
TEAR TEST TRANSITION TEMPERATURE, °F.
CARBON CONTENT, PER CENT
¾ - INCH SEMIKILLED CLASS A STEEL
FINISHED AT 1650 °F.
FINISHED AT 1850 °F.

Fig. 4 Influence of carbon content upon the transition temperature of class A laboratory steels

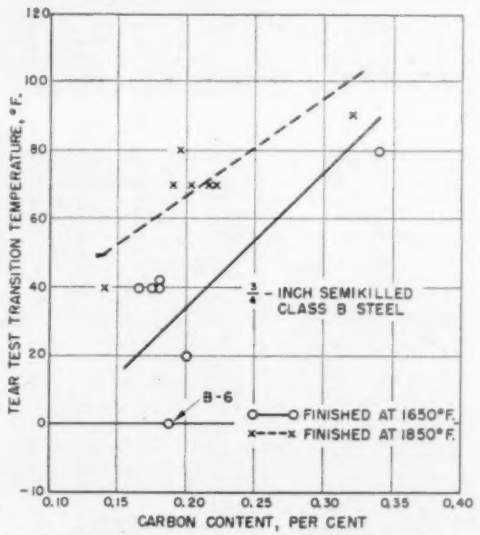

Fig. 5 Influence of carbon content upon the transition temperature of class B laboratory steels

Table 7—Chemical Analysis, Tensile, Tear Test, and Keyhole Charpy Impact Properties of 3/4-In., Hot-Rolled Laboratory Steel Plates Made to Study the Influence of Carbon and Manganese Content. Finishing Temperature 1650° F.

Identification of steel			Chemical analysis, Composition, %						Tensile properties				Tear test properties			
Grade of steel	Heat No.	Location of sample	C	Mn	P	S	Si	N	Yield strength, psi., upper*	Tensile strength, psi.	Elongation in 8 in., %	Maximum load, lb.	Energy to start fracture,† ft.-lb.	Energy to propagate fracture,† ft.-lb.	Transition temperature, °F.	Charpy impact transition temperature,‡ °F.
(Carbon series)																
Class A	A6293	Top	0.12	0.49	0.019	0.025	0.04	0.005	35,600	51,750	36.0	37,610	1240	940	+10	−9
		Bottom	0.12	0.48	0.015	0.025	0.04	0.004								
Class A	A6294	Top	0.37	0.53	0.018	0.024	0.03	0.004	43,450	72,950	26.0	39,810	650	480	+90	+77
		Bottom	0.36	0.55	0.018	0.025	0.03	0.004								
Class B	A6155	Top	0.22	0.87	0.022	0.028	0.02	0.005	41,150	62,110	31.5	41,660	1050	830	+20	−45
		Bottom	0.18	0.86	0.019	0.025	0.02	0.006								
Class B	A6292	Top	0.35	1.02	0.019	0.024	0.04	0.004	50,650	84,300	23.0	47,380	740	560	+80	+43
		Bottom	0.33	1.02	0.020	0.024	0.04	0.006								
(Manganese series)																
Class A	A6295	Top	0.20	0.29	0.018	0.025	0.03	0.004	36,000	56,050	33.0	35,810	840	690	+90	+28
		Bottom	0.20	0.29	0.018	0.026	0.03	0.004								
Class A	A6296	Top	0.19	1.30	0.020	0.024	0.07	0.006	45,800	72,400	28.0	47,390	1140	720	+20	−66
		Bottom	0.22	1.27	0.019	0.024	0.05	0.004								
Class B	A6159	Top	0.25	0.37	0.021	0.026	0.01	0.005	36,550	56,900	31.5	37,481	1010	670	+60	+21
		Bottom	0.18	0.38	0.015	0.023	0.01	0.004								
Class B	A6306	Top	0.16	1.48	0.023	0.024	0.09	0.006	45,550	69,050	30.5	47,800	1280	860	+20	−75
		Bottom	0.19	1.40	0.021	0.025	0.04	0.004								

* Upper yield strength—the maximum strength before "drop of the beam."
† Average energy of the tests made at 10° F. higher than transition temperature.
‡ Temperature at which impact strength is 20 ft.-lb.

Table 8—Chemical Analysis, Tensile, Tear Test, and Keyhole Charpy Impact Properties of 3/4-In., Hot-Rolled Laboratory Steel Plates Made to Study the Influence of Carbon and Manganese Content. Finishing Temperature 1850° F.

Identification of steel			Chemical analysis, Composition, %						Tensile properties				Tear test properties			
Grade of steel	Heat No.	Location of sample	C	Mn	P	S	Si	N	Yield strength, psi. Upper*	Yield strength, psi. Lower*	Tensile strength, psi.	Elongation in 8 in., %	Maximum load, lb.	Energy to start fracture,† ft.-lb.	Energy to propagate fracture,† ft.-lb.	Transition temperature, °F.	Charpy impact transition temperature,‡ °F.
(Carbon series)																	
Class A	A6539	Top	0.16	0.42	0.018	0.028	0.02	0.004	31,850	30,750	53,300	30.5	34,970	930	690	60	+10
		Bottom	0.15	0.41	0.017	0.027	<0.01	0.003									
Class A	A6596	Top	0.35	0.50	0.016	0.023	0.06	0.003	41,300	38,550	72,900	21.0	36,470	520	570	120	+75
		Bottom	0.34	0.48	0.015	0.023	0.07	0.004									
Class B	A6586	Top	0.14	0.77	0.011	0.024	0.07	0.004	33,000	32,000	54,400	28.0	38,490	1270	1110	40	−24
		Bottom	0.14	0.76	0.011	0.023	0.07	0.003									
Class B	A6597	Top	0.33	0.79	0.017	0.024	0.06	0.004	40,900	40,100	75,100	24.5	40,970	640	610	90	+19
		Bottom	0.31	0.81	0.017	0.024	0.07	0.004									
(Manganese series)																	
Class A	A6589	Top	0.22	0.26	0.016	0.024	0.07	0.004	34,050	33,050	58,400	29.5	35,180	820	670	100	+36
		Bottom	0.22	0.26	0.016	0.025	0.09	0.005									
Class A	A6598	Top	0.24	1.31	0.019	0.026	0.07	0.004	42,850	42,150	74,200	23.0	45,490	950	830	70	−60
		Bottom	0.24	1.24	0.014	0.026	0.07	0.004									
Class B	A6590	Top	0.19	0.22	0.015	0.027	0.04	0.004	33,100	31,450	55,100	30.5	34,110	840	740	90	+26
		Bottom	0.19	0.22	0.015	0.026	0.05	0.003									
Class B	A6599	Top	0.21	1.46	0.013	0.023	0.07	0.004	43,850	43,400	72,350	24.5	46,820	970	850	60	−38
		Bottom	0.20	1.47	0.017	0.022	0.06	0.004									

* Upper yield strength—the maximum strength before "drop of the beam," lower yield strength—lowest strength during "drop of the beam."
† Average energy of tests made at 10° F. higher than transition temperature.
‡ Temperature at which impact strength is 20 ft.-lb.

Table 9—Chemical Analysis, Tensile, Tear Test, and Keyhole Charpy Impact Properties of 3/4-In., Hot-Rolled Laboratory Steel Plate with Various Phosphorus and Sulphur Contents. Finishing Temperature 1850° F.

Identification of steel			Chemical analysis						Tensile properties				Tear test properties				
			Composition, %						Yield strength, psi.								
Grade of steel	Heat No.	Location of sample	C	Mn	P	S	Si	N	Upper*	Lower*	Tensile strength, psi	Elongation in 8 in., %	Maximum load, lb.	Energy† to start fracture, ft.-lb.	Energy to† propagate fracture, ft.-lb.	Transition temperature, ° F.	Charpy impact‡ transition temperature, ° F.
(Phosphorus series)																	
Class A	A6135	Top	0.23	0.49	0.012	0.022	0.04	0.004	34,750	33,100	59,100	28.5	35,380	710	700	80	−1
		Bottom	0.22	0.47	0.009	0.025	0.03	0.004									
Class A	A6652	Top	0.19	0.49	0.038	0.020	0.02	0.007	37,350	36,250	64,000	23.5	38,730	850	580	110	+20
		Bottom	0.24	0.49	0.038	0.024	0.02	0.005									
Class A	A6706	Top	0.28	0.47	0.058	0.028	0.06	0.004	39,650	38,500	67,250	21.0	38,120	730	640	110	+50
		Bottom	0.22	0.49	0.049	0.026	0.07	0.004									
Class B	A6638	Top	0.16	0.80	0.016	0.018	0.05	0.007	36,000	34,150	61,650	28.5	38,820	940	690	60	−26
		Bottom	0.18	0.75	0.015	0.022	0.05	0.004									
Class B	A6653	Top	0.20	0.76	0.046	0.023	0.06	0.005	38,600	36,550	64,750	25.0	39,710	880	750	90	−11
		Bottom	0.19	0.76	0.045	0.023	0.06	0.004									
Class B	A6655	Top	0.13	0.81	0.052	0.016	0.10	0.010	40,800	38,250	67,450	23.5	41,150	880	640	110	+10
		Bottom	0.19	0.81	0.054	0.022	0.04	0.005									
(High-sulphur steels)																	
Class A	A6647	Top	0.20	0.53	0.017	0.038	0.07	0.006	38,600	35,550	62,000	26.0	36,390	700	670	60	+10
		Bottom	0.22	0.54	0.016	0.046	0.07	0.005									
Class B	A6646	Top	0.20	0.83	0.017	0.046	0.08	0.005	37,400	36,150	62,150	28.5	39,090	900	720	50	−24
		Bottom	0.19	0.81	0.016	0.044	0.07	0.005									

* Upper yield strength—the maximum strength before "drop of the beam," lower yield strength—lowest strength during "drop of the beam."
† Average energy of tests made at 10° F. higher than transition temperature.
‡ Temperature at which the impact strength is 20 ft.-lb.

Table 10—Chemical Analysis, Tensile, Tear Test, and Keyhole Charpy Impact Properties of 3/4-In., Hot-Rolled Laboratory Steel Plates with Various Silicon Contents. Finishing Temperature 1850° F.

Identification of steel			Chemical analysis						Tensile properties				Tear test properties				
			Composition, %						Yield strength, psi.								
Grade of steel	Heat No.	Location of sample	C	Mn	P	S	Si	N	Upper*	Lower*	Tensile strength, psi.	Elongation in 8 in., %	Maximum load, lb.	Energy† to start fracture, ft.-lb.	Energy to† propagate fracture, ft.-lb.	Transition temperature, ° F.	Charpy impact‡ transition temperature, ° F.
Silicon series)																	
Class A	A6602	Top	0.24	0.51	0.011	0.019	0.02	0.004	33,550	32,950	60,000	30.5	36,290	770	690	80	+14
		Bottom	0.22	0.51	0.010	0.017	0.02	0.003									
Class A	A6594	Top	0.22	0.48	0.019	0.027	0.11	0.003	37,100	35,700	62,450	30.0	38,270	810	830	80	−7
		Bottom	0.21	0.48	0.017	0.027	0.11	0.003									
Class A	A6657	Top	0.23	0.48	0.016	0.021	0.15	0.004	34,200	33,650	63,350	26.5	38,140	840	650	70	−2
		Bottom	0.23	0.49	0.016	0.021	0.14	0.004									
Class A	A6696	Top	0.25	0.55	0.015	0.023	0.31	0.005	37,400	36,650	67,200	26.0	40,390	800	740	70	−28
		Bottom	0.24	0.54	0.016	0.023	0.31	0.005									
Class B	A6603	Top	0.21	0.84	0.016	0.022	0.03	0.004	37,550	36,850	64,900	23.0	39,090	870	730	80	−29
		Bottom	0.21	0.84	0.018	0.022	0.02	0.003									
Class B	A6595	Top	0.20	0.83	0.017	0.024	0.13	0.004	38,350	36,150	63,400	27.5	40,870	1020	720	40	−43
		Bottom	0.20	0.80	0.017	0.024	0.12	0.004									
Class B	A6695	Top	0.20	0.84	0.016	0.024	0.16	0.004	38,450	36,700	64,300	26.5	42,120	940	710	30	−57
		Bottom	0.19	0.83	0.014	0.025	0.16	0.004									
Class B	A6697	Top	0.19	0.85	0.015	0.023	0.29	0.005	39,050	37,800	66,900	29.5	42,390	970	730	60	−29
		Bottom	0.19	0.86	0.016	0.023	0.29	0.005									

* Upper yield strength—the maximum strength before "drop of the beam," lower yield strength—lowest strength during "drop of the beam."
† Average energy of tests made at 10° F. higher than transition temperature.
‡ Temperature at which impact strength is 20 ft.-lb.

Table II—Chemical Analysis, Tensile, Tear Test, and Keyhole Charpy Impact Properties of 3/4-In., Hot-Rolled Laboratory Steel Plate with Various Vanadium Contents. Finishing Temperature 1850° F.

Identification of steel			Chemical analysis								Tensile properties				Tear test properties			Charpy impact transition temperature, °F.‡
			Composition, %								Yield strength, psi.—		Tensile strength, psi.	Elongation in 8 in., %	Maximum load, lb.	Energy† to start fracture, ft.-lb.	Energy to† propagate fracture, ft.-lb.	Transition temperature, °F.
Grade of steel (Vanadium series)	Heat No.	Location of sample	C	Mn	P	S	Si	N	V		Upper*	Lower*						
Class A	A6642	Top	0.22	0.50	0.016	0.022	0.07	0.003	0.06		44,950	40,850	67,900	23.0	39,080	650	620	+15
		Bottom	0.21	0.49	0.015	0.023	0.09	0.004	0.10									
Class A	A6368	Top	0.21	0.48	0.019	0.026	0.04	0.003	0.09		45,300	44,450	68,000	25.0	40,760	640	670	+10
		Bottom	0.23	0.49	0.016	0.027	0.03	0.002	0.09									
Class A	A6366	Top	0.19	0.49	0.015	0.022	0.06	0.002	0.20		57,300	53,900	78,400	20.0	45,800	523	510	+73
		Bottom	0.23	0.50	0.015	0.028	0.04	0.002	0.18									
Class B	A6643	Top	0.20	0.82	0.016	0.023	0.10	0.003	0.08		43,900	42,550	68,550	22.0	42,210	800	760	70	−25
		Bottom	0.19	0.81	0.016	0.023	0.08	0.003	0.08									
Class B	A6644	Top	0.18	0.83	0.016	0.021	0.08	0.004	0.12		51,250	48,900	73,200	18.5	44,100	690	780	100	−20
		Bottom	0.20	0.82	0.016	0.022	0.09	0.003	0.12									
Class B	A6645	Top	0.18	0.80	0.016	0.023	0.10	0.003	0.20		63,000	60,250	85,050	15.5	52,060	530	550	160	+70
		Bottom	0.20	0.78	0.017	0.021	0.10	0.002	0.19									

* Upper yield strength—the maximum strength before "drop of the beam," lower yield strength—lowest strength during "drop of the beam."
† Average energy of tests made at 10° F higher than transition temperature.
‡ Temperature at which impact strength is 20 ft.-lb.

erally accepted as indicative of actual service performance, has been obtained with tear tests, special attention is given to the tear test transition data in this work. The indications are that the cold work performed in the tear test prior to rupture is probably a significant factor in the small-scale test. For comparison purposes, however, the Charpy impact transition temperature has also been included for each steel studied.

The effect of carbon content upon the tear test transition temperature for Class A steel is shown in Fig. 4. These data show that the transition temperature increases rapidly as the carbon content is raised. For the steels finished at 1850° F., an increase in carbon content from 0.16 to 0.35% raised the transition temperature from 60 to 120° F. The carbon content has a similar influence upon the steels finished at 1650° F., however, the lower finishing temperature depresses the transition temperature about 30° F. From Fig. 5, it will be noted that the carbon content exerts a similar effect in the Class B steels, but apparently slightly less pronounced.

Fig. 6 Influence of manganese content upon the transition temperature of laboratory steels

The influence of manganese content upon the transition temperature is shown in Fig. 6. It is significant to note that increasing the manganese lowers the transition temperature by an appreciable extent in both the Class A and B steels. This figure again illustrates the effect of finishing temperature.

The relationship between yield strength and transition temperature in the Class A steels where the strength level was controlled by varying the carbon content is illustrated in Fig. 7. These limited data indicate that increasing the yield strength 8000 psi. by raising the carbon content increases the transition temperature about 80° F., or roughly 10° F. per 1000 psi.

Similar data are shown in Fig. 8 for the Class A steels in which the yield strength was varied by changing the manganese content. In this case, it will be observed that increasing the yield strength 8000 psi., by raising

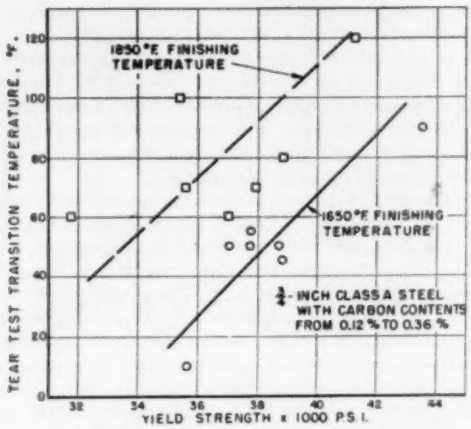
1850°F FINISHING TEMPERATURE
1650°F FINISHING TEMPERATURE
¾-INCH CLASS A STEEL WITH CARBON CONTENTS FROM 0.12 % TO 0.36 %
TEAR TEST TRANSITION TEMPERATURE, °F.
YIELD STRENGTH x 1000 P.S.I.

Fig. 7 Relationship between yield strength and transition temperature when yield strength was varied by changing the carbon content

the manganese content, lowered the transition temperature about 40° F.

While the relationship between yield strength and transition temperature, as influenced by carbon and manganese content, is based on limited data so that the absolute values are not too significant, these data do indicate that the most desirable yield strength to transition temperature ratio is achieved by obtaining a considerable portion of the required strength from the manganese content and reducing the carbon content to a minimum.

It appears that silicon content has little or no effect upon the transition temperatures of Class A steels. From the data in Fig. 9, it appears, however, that

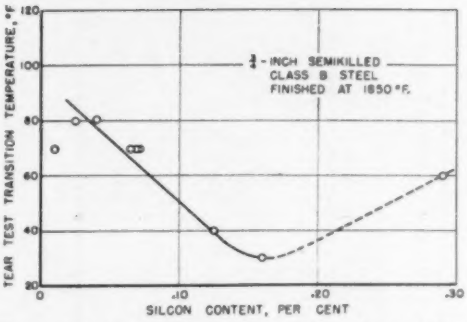

Fig. 9 Influence of silicon content upon the transition temperature of class B laboratory steels

increased silicon content up to about 0.20% lowers the transition temperature, while additional silicon causes the transition temperature to increase. In the case of killed steels, Rinebolt and Harris[3] report that, as the silicon content is increased above about 0.26%, the transition temperature is raised. This appears to be in agreement with the results obtained from the higher silicon Class B heats.

The data in Figs. 10 and 11 show that the transition temperature is very definitely raised by increasing the phosphorus content, the effect being similar in both classes of steel. The indications are that the range of phosphorus content that may be encountered in commercial ship plate is sufficiently broad to have perceptible effect on the transition temperature.

The indications are, from the limited data, that sulphur has little effect on, or may possibly lower to a slight extent, the transition temperature of Class A and B steels. From these data, it appears that the range

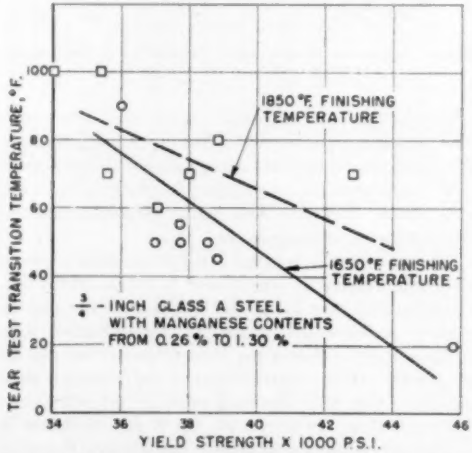
1850°F FINISHING TEMPERATURE
1650°F FINISHING TEMPERATURE
¾-INCH CLASS A STEEL WITH MANGANESE CONTENTS FROM 0.26 % TO 1.30 %
TEAR TEST TRANSITION TEMPERATURE, °F.
YIELD STRENGTH X 1000 P.S.I.

Fig. 8 Relationship between yield strength and transition temperature when the yield strength was varied by changing the manganese content

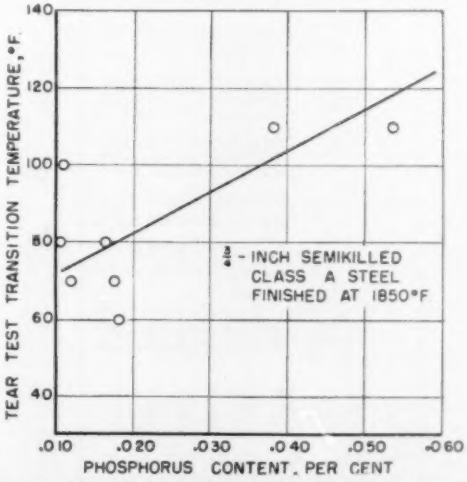

Fig. 10 Influence of phosphorus content upon the transition temperature of class A laboratory steels

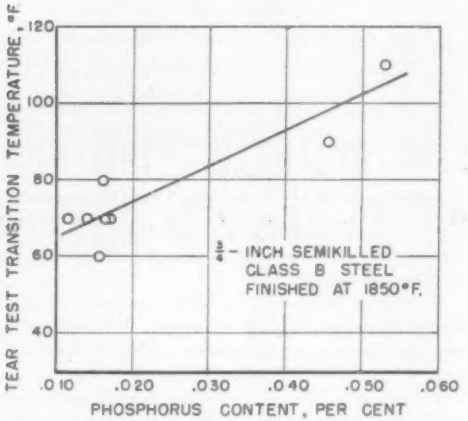

Fig. 11 Influence of phosphorus content upon transition temperature of class B laboratory steels

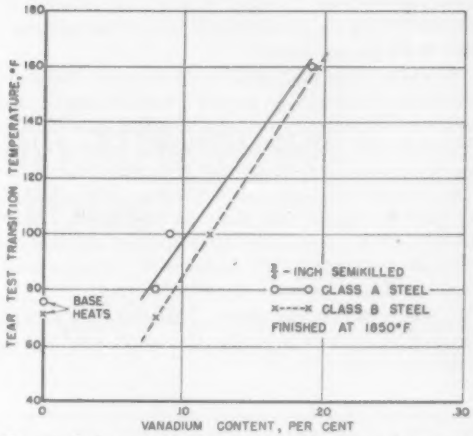

Fig. 12 Influence of vanadium content upon the transition temperature of class A and class B laboratory steels

of sulphur encountered in commercial steels would have no significant influence on the transition temperature.

Vanadium was found to have a pronounced effect upon the transition temperature, the transition temperature being raised by the addition of vanadium. This marked effect of vanadium is shown in Fig. 12, which indicates that the addition of 0.20% vanadium raised the transition temperature by about 80° F., with smaller additions having a proportionate effect.

SUMMARY

The data presented in this paper show that 200-lb. semikilled laboratory heats can be made with ample reproducibility for use in studying the influence of chemical composition and deoxidation upon the transition-temperature characteristics of ABS class A and B plate steels.

The transition temperature of steels of the class A and B type was found to be progressively raised, and to an appreciable extent, by increasing the carbon, phosphorus and vanadium contents within the limits studied in this investigation.

Limited data indicated that increased sulphur did not raise the transition temperature but possibly lowered it. In the range that sulphur occurs in commercial steels, it would not be expected to have a significant effect.

In order to establish definitely the effect of silicon content, more data are needed. It does appear, however, that it is safe to assume that increased silicon content, within the range covered in this investigation, does not raise the transition temperature.

The transition temperature was definitely lowered by increasing the manganese content in the range covered in this investigation. The transition temperature was also lowered by decreasing the finishing temperature of the hot-rolled plate, the effect being quite pronounced.

Since the work on this project has not been completed, this paper is essentially a progress report.

ACKNOWLEDGMENT

The authors wish to express their appreciation to the Ship Structure Committee, who sponsored this work, and to the Committee on Ship Steel, under whose guidance the project was conducted, for granting permission to publish these results. The authors are also grateful for the help received from the Battelle Institute staff in conducting the project and in preparing this paper.

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Discussion by James W. Halley

It is very encouraging to see studies of this sort being made. There has been such emphasis on the mechanics of fracture and on test procedures that knowledge of the metallurgical factors involved is very meager.

The detrimental effect of carbon is well established. Kinzel[1] using a different testing procedure and a different criterion for transition temperature found that 0.10% carbon raised the transition temperature 40° F. This is in almost exact agreement with the slope of the curves in the present paper.

The effect of grain size is not so clear though Hodges'[2] values of 30° F. decrease in transition temperature for

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each unit increase in A.S.T.M. grain size number is consistent with the change caused by finishing temperature in the present report.

It is to be hoped that a grain size correction will reduce the scatter in some of the curves. As the authors point out, steel B6 is out of line because it has an unusually fine grain size. It is possible that the effect of manganese is somewhat exaggerated by a grain size effect. The 1.25 and 1.47% manganese steels would be expected to have finer grain sizes than the base steels. Barr[3] found a 40° F. decrease in Izod transition temperature with an increase of 0.89% manganese. This is a good deal less than is shown in the present paper.

A comparison of the effects of elements that should be in solid solution as reported by various authors is tabulated below.

Element	*Change in transition temperature with each 0.10% increment added, ° F.*	*Method of test*	*Source*
Manganese	−8	Kahn tear test	Banta
	−4	Izod	Barr[3]
	−10	Charpy V notch	Rinebolt[4]
Nickel	−2	Charpy keyhole notch	Hodge[2]
	−1	Charpy V notch	Rinebolt[4]
Phosphorous	+100	Kahn tear test	Banta
	+130	Charpy V notch	Rinebolt[4]
Vanadium	+80	Kahn tear test	Banta
	+80	Charpy V notch	Rinebolt[4]
Silicon	+12.5	Charpy V notch	Rinebolt[4]

Despite the variations in testing procedure the results are remarkably consistent. As far as the data goes it appears that elements in solid solution that expand the gamma field lower the transition temperature while elements that form a gamma loop raise the transition temperature. This relationship is almost quantitative. Phosphorus prevents the formation of gamma iron at 0.5%, vanadium at 1.1% and silicon at 3.5%. We can guess that tin and antimony should raise the transition temperature about as fast as vanadium while molybdenum will raise it slowly and tungsten still more slowly. Small amounts of copper, under 0.35%, should lower the transition temperature.

The elements that lower the transition temperature require relatively large quantities to produce an appreciable change. Since highly alloyed steels can hardly be considered for ship construction solid solution alloying is an unpromising approach to the ship steel problem.

The large effect of grain size on transition temperature, however, presents a very promising avenue for improvement. If the action of grain growth inhibition were understood it is quite possible that a steel could be made which was semikilled and still fine grained when hot rolled at normal temperatures. It is certain that control of rolling temperatures can produce a marked decrease of grain size of present steels though this can probably not be done on existing rolling mills.

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4. Rinebolt, J. A., and Harris, W. J., Jr., "Effect of Alloying Elements on Notch Toughness of Pearlitic Steels," *A.S.M. Preprint* No. 33 (1950).

International Welding Commissions

(*Continued from page 78-s*)

defects this influence may be completely masked by the effect of defects.

The Commission came to the conclusion that in assemblies larger than those tested, the influence of residual stresses may be more pronounced, especially under conditions where plastic adaptation is prevented.

In a report submitted by France on work carried out by M. G. Dawance, it is shown that residual stresses exercise an influence on deformation, irrespective of whether they are due to rolling or welding. After the first loading cycle this effect disappears if the loading is not reversed.

Commission No. 12—Brittle Fractures

Vice-Chairman: A. GOELZER

It had been decided at the Delft meetings of Commission No. 12 under the Chairmanship of Dr. Haringx, that its work should be divided between two Sub-Commissions whose studies would run parallel. The task of Sub-Commission 12a, whose rapporteur is Mr. Herbiet, is to collect, study and interpret information on fractures and on tests. Sub-Commission 12b, of which Dr. Haringx is rapporteur, is concerned with the theoretical study of brittle fractures. The object of organizing the work in this way was to enable progress to be made simultaneously with experimental and theoretical subjects.

At the beginning of this session's meetings an examination of the various documents with which all the members were acquainted was carried out. The principal points brought to light by this examination are the following:

1. The majority of the documents referred to the contents recommended in the composition of steels not susceptible to brittle fracture. The importance of the report Mn/C was pointed out in particular.

2. The tests generally used to discover the susceptibility of steel to brittle fracturing are:

> static tensile tests on test pieces either notched or with weld beads; impact bend tests on notched bars.

These tests are often carried out in different temperatures so as to try to determine the transition temperature.

3. Mr. de Loiris underlined the importance of the amount of potential energy stores and as a consequence the more or less great danger of susceptibility

(*Continued on page 104-s*)

The Determination of Initial Stresses and Results of Tests on Steel Plates

Evaluation of a nondestructive method for determining initial stresses, theory, test results and recommended procedures

Part I. The General Problem and Evaluation of the Method Used

by E. W. Suppiger

INTRODUCTION

NUMEROUS research investigations have demonstrated the existence of high initial stresses in the vicinity of welds. A simple, accurate, and nondestructive method for the determination of such stresses in steel plates is presented in this paper. The term initial stress (alias residual, locked-in or inherent stress) refers to the stress in a solid body which is not caused by external loads (live loads) or body forces (gravity and inertia). Initial stresses may be developed during processing or fabrication; those due to welding are discussed most frequently. This paper deals with one method (the hole method) whereby initial stresses may be determined but makes no attempt to cover their origin, nature, significance, control or removal.

Preliminary work on the development of a nondestructive method utilizing a small hole and wire strain gages to determine, by a relaxation process, the initial stresses in steel plates was carried out at Princeton University by C. Riparbelli from 1946–48 under a grant from the Research Corp. Results of this study were submitted in report form to the Research Corp. and presented before the Society for Experimental Stress Analysis at the New York meeting in December 1947.

Presented at Thirty-first Annual Meeting, A.W.S., Chicago, Ill., week of Oct. 22, 1950.

E. W. Suppiger, School of Engineering, Princeton University, Princeton, N. J.

Opinions expressed are those of the authors and not necessarily those of the Bureau of Ships, Navy Department and the Ship Structure Committee.

In February 1949, a research investigation on initial stresses was begun at the School of Engineering, Princeton University, for the Ship Structure Committee, under a contract with the Navy Department, Bureau of Ships, Contract number NObs-47613. The official title of the project was "The Determination of Initial Stresses" and the Navy project number was NS-011-077. Work under this contract ended on June 30, 1950. An advisory committee with representatives of the U. S. Navy, U. S. Army, U. S. Coast Guard, U. S. Maritime Commission and the American Bureau of Shipping assisted throughout the investigation.

The major items of work accomplished were:

1. Development of the techniques of the hole method for determining initial stresses to a stage where the method can be considered practical for field tests.
2. Performance of numerous calibration tests (primarily tensile) with holes from $^1/_8$ to 1 in. in diameter on steel plates varying in thickness from $^3/_8$ to $1^1/_4$ in.
3. Investigation of several loading conditions and stress ranges and comparison of results to those: (*a*) found by the plug method, and (*b*) calculated from known loads.

SUMMARY

The results of this investigation are presented here in three parts:

Part I. An introduction to the problem and an outline and evaluation of the hole method utilizing SR-4 A-7 electric resistivity strain gages (gage length $^1/_4$ in.).

Part II. The pertinent theory—expressions are given for the stresses and strains in plates, with and without small circular holes, subjected to biaxial tension or compression. From these are obtained the strain differences and the coefficients of sensitivity for both strains and stresses. The corresponding problem in bending of plates is discussed briefly.

Part III. Several testing techniques and methods of calculation used in the evaluation of tensile coefficients of sensitivity for stress are described. Calibration and field-type tests are explained and results obtained by the hole method using experimental coefficients are compared to known stresses. A recommended field procedure for the determination of the unknown principal stresses in plates is stated.

DISCUSSION

Initial stresses are usually determined experimentally (calculation, in most cases, is not possible) in one of two ways: (1) by a relaxation process, or (2) by X-ray diffraction for opaque bodies or polarized light for transparent bodies. The former method includes sectioning, machining, the plug method, the drilling of holes, etc. The hole method was used in this investigation and has the following desirable characteristics:

1. A minimum amount of material is removed by the drilling of a small hole.
2. The strain relaxation is complete from no hole to hole condition.
3. Application to field tests is relatively simple.

Both calibration tests and field-type tests were performed on steel plates sub-

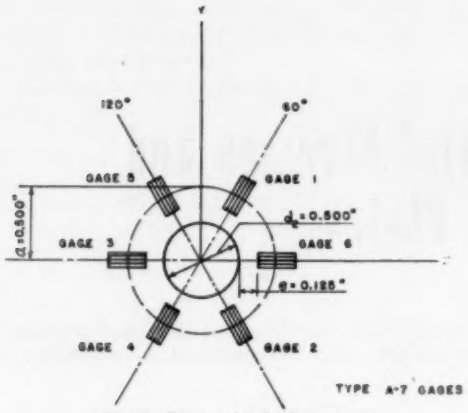

Fig. 1 Rosette for field-type test

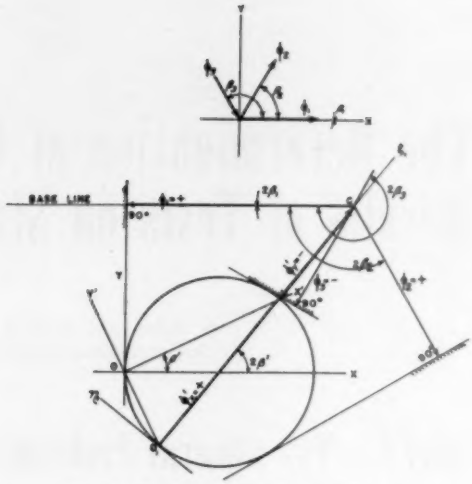

Fig. 2 Dyadic strain-circle construction

jected to (a) tension and (b) bending. The major variables studied were: (1) plate thickness, width, length, (2) hole size, (3) distance from edge of hole to edge of A-7 gages, (4) arrangement of gages and (5) method of drilling the hole.

The hole method was first proposed and applied by J. Mathar[1, 2] of the University of Aachen in 1932. Mathar used mechanical and optical extensometers to measure the changes in displacement between points on the surface of members when a hole was drilled between the points.

The hole method as reported in this paper may be outlined as follows: Apply to the surface of the plate (on each side) a strain rosette centered on the stress point. A typical rosette for a field type test is shown in Fig. 1. A small hole is then drilled at the center of the rosette. From the strain readings taken before and after the drilling of the hole, the principal stresses in the plate *before* the hole was drilled may be computed.

The rosette is used to measure the strain differences in three directions. From these strain readings one may determine, in magnitude and direction, the two unknown principal strain differences. The dyadic strain-circle construction,[3] illustrated in Fig. 2, was employed for this purpose, several methods are available and may be preferred by others to solve this problem. The two unknown principal stresses in the solid plate are then computed from two simple linear equations involving the two principal strain differences and two experimental constants λ_1 and λ_2 (the coefficients of sensitivity for stress) found by calibration tests.

The procedure to be followed is much the same for biaxial tension or bending. When both are present, one superposes to obtain the final result. The laboratory setups for calibration tests in tension and bending are shown in Figs. 3 and 4, respectively.

The following remarks concerning the hole method are apropos:

1. The method is considered nondestructive inasmuch as only a small hole is needed. After a stress survey, the holes may be filled with weld metal or rivets.

2. A uniform state of stress is assumed in the plate around the stress point before the hole is drilled. The method may thus lead to inaccurate results in regions with steep stress gradients. However, smaller holes, gages, etc. (with associated experimental λ_1 and λ_2) may be selected in such cases.

3. Satisfactory sensitivity is obtainable with the radial orientation of gages shown in Fig. 1. At high stress levels, the strains in the direction tangent to the hole are critical. Thus when the plate undergoes plastic action, the performance of the radial gages will be less affected. To expedite field testing, complete radial rosettes could, of course, be obtained and used.

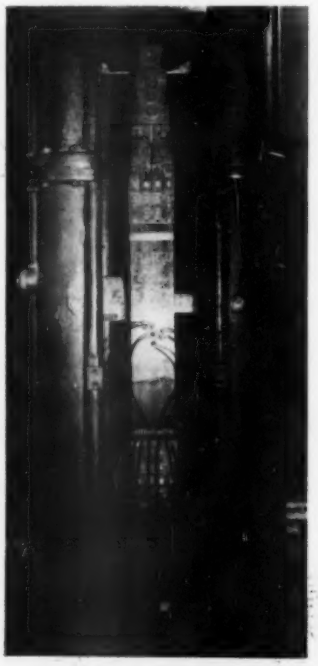

Fig. 3 Typical tension test

CONCLUSION

1. The hole method produces complete release of the strains. The stresses obtained are the sum of initial stresses plus any stresses due to external loads and body forces. The method is therefore not limited to the determination of initial stresses (such as caused by fabrication, welding, etc.). For accuracy of the method, see Part III.

2. The assumptions made in Part II of homogeneity and isotropy, of linearity between strain and stress, and of the principle of superposition appear to be justified by the test results.

3. All strain gage data, using the techniques discussed in Part III, are obtained within an allowable error of 10 microinches per inch.

4. Initial stresses in the order of 2500 psi. were found in plates stress relieved at 1200° F. It should be stated here that no method was available to the investigators as a "standard" whereby the hole method could be compared.

5. Further test work, both in the

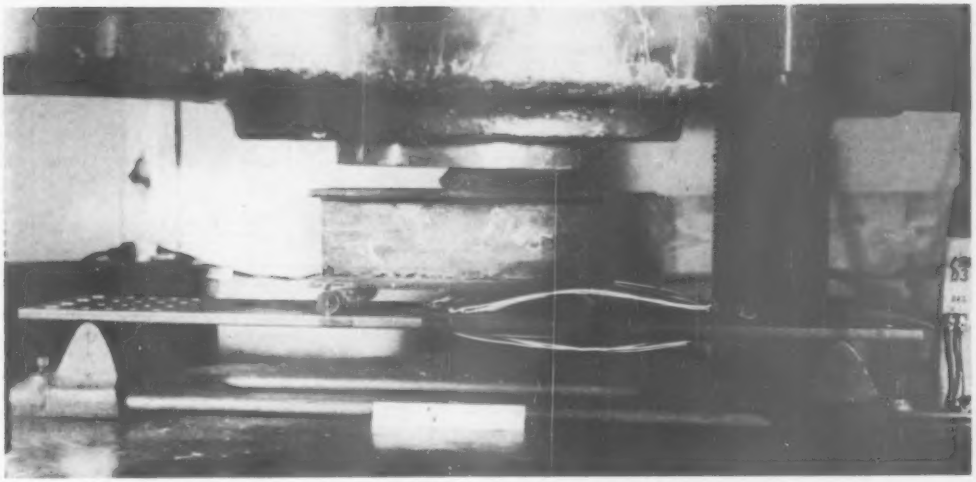

Fig. 4 Typical bending test

laboratory and in the field, should be performed using the hole method on: (*a*) plates of different thicknesses subjected to known biaxial and bending loads with various ratios of σ_1 to σ_2, (*b*) steep stress gradient fields and (*c*) high stress levels.

Part II. The Method of the Hole

by C. Riparbelli

INTRODUCTION

SINCE the strain distribution in a plate with a small circular hole subjected to a given biaxial tensile or compressive load is well known, with limitations to be discussed later, it follows that the relationship between: (*a*) the strain distribution in the presence of a hole, and (*b*) the strain distribution in a solid plate is known as a function of the applied loads. From the difference of (*a*) and (*b*) the stresses in a solid plate may be computed.

The analysis that follows is limited to plates loaded in either of two ways:

1. Biaxial tension or compression.
2. Bending.

BIAXIAL TENSION OR COMPRESSION

For the assumed plane state of stress: $\sigma_z = \tau_{xz} = \tau_{zy} = 0$ and σ_x, σ_y and τ_{xy} are independent of z (normal to xy-plane of the plate). For a uniformly distributed load σ_1 applied parallel to the g-axis, Kirsch's equations[1] give the state of stress at any point in the plate with a small circular hole, see Fig. 5. At a large distance from the hole (where the local influence

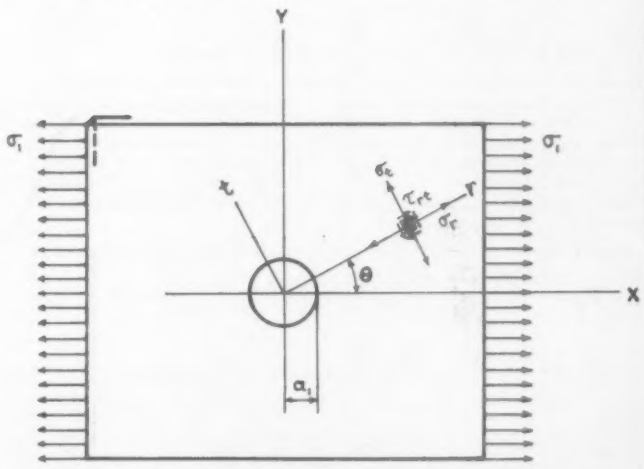

Fig. 5 Uniaxial loading of plate with small circular hole

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of the hole may be neglected) the only nonzero principal stress is σ_1.

Let:

- σ_r = normal stress in the radial direction.
- σ_t = normal stress orthogonal to the radius.
- τ_{rt} = shearing stress referred to the r and t axes.
- a_1 = radius of the hole.
- r = distance from center of hole to the point considered.
- θ = angle between the x and r axes, measured counterclockwise.

Kirsch's equations are:

$$\sigma_r = \frac{\sigma_1}{2}\left(1 - \frac{a_1^2}{r^2}\right) + \frac{\sigma_1}{2}\left(1 + 3\frac{a_1^4}{r^4} - 4\frac{a_1^2}{r^2}\right)\cos 2\theta$$

$$\sigma_t = \frac{\sigma_1}{2}\left(1 + \frac{a_1^2}{r^2}\right) - \frac{\sigma_1}{2}\left(1 + 3\frac{a_1^4}{r^4}\right)\cos 2\theta \qquad (1)$$

$$\tau_{rt} = -\frac{\sigma_1}{2}\left(1 - 3\frac{a_1^4}{r^4} + 2\frac{a_1^2}{r^2}\right)\sin 2\theta$$

If the plate is acted upon by a distributed load σ_2 in the y-direction, Equations 1 may be written for this case ($\sigma_1 = 0$, $\sigma_2 \neq 0$) by substituting $\frac{3\pi}{2} + \theta$ for θ.

By superposition of the above two cases, the stress for biaxial loading ($\sigma_1 \neq 0$, $\sigma_2 \neq 0$) is found to be:

$$\sigma_r = \frac{(\sigma_1+\sigma_2)}{2}\left(1 - \frac{a_1^2}{r^2}\right) + \frac{(\sigma_1-\sigma_2)}{2}\left(1 + 3\frac{a_1^4}{r^4} - 4\frac{a_1^2}{r^2}\right)\cos 2\theta$$

$$\sigma_t = \frac{(\sigma_1+\sigma_2)}{2}\left(1 + \frac{a_1^2}{r^2}\right) - \frac{(\sigma_1-\sigma_2)}{2}\left(1 + 3\frac{a_1^4}{r^4}\right)\cos 2\theta \qquad (2)$$

$$\tau_{rt} = -\frac{(\sigma_1-\sigma_2)}{2}\left(1 - 3\frac{a_1^4}{r^4} + 2\frac{a_1^2}{r^2}\right)\sin 2\theta$$

The stress components referred to the xy axes are next obtained for the biaxial loading being considered. From these the strain components in the x and y directions will be computed. The expressions for σ_x, σ_y, and τ_{xy} at any point in terms of σ_r, σ_t, τ_{rt}, and θ are:

$$\sigma_x = \frac{(\sigma_r+\sigma_t)}{2} + \frac{(\sigma_r-\sigma_t)}{2}\cos 2\theta - \tau_{rt}\sin 2\theta$$

$$\sigma_y = \frac{(\sigma_r+\sigma_t)}{2} - \frac{(\sigma_r-\sigma_t)}{2}\cos 2\theta + \tau_{rt}\sin 2\theta \qquad (3)$$

$$\tau_{xy} = \frac{(\sigma_r-\sigma_t)}{2}\sin 2\theta + \tau_{rt}\cos 2\theta$$

From Equations 2 and 3 it follows that:

$$\sigma_x = \frac{(\sigma_1+\sigma_2)}{2}\left(1 - \frac{a_1^2}{r^2}\cos 2\theta\right) + \frac{(\sigma_1-\sigma_2)}{2}\left[1 - 2\frac{a_1^2}{r^2}\cos 2\theta + \left(3\frac{a_1^4}{r^4} - 2\frac{a_1^2}{r^2}\right)\cos 4\theta\right]$$

$$\sigma_y = \frac{(\sigma_1+\sigma_2)}{2}\left(1 + \frac{a_1^2}{r^2}\cos 2\theta\right) - \frac{(\sigma_1-\sigma_2)}{2}\left[1 + 2\frac{a_1^2}{r^2}\cos 2\theta + \left(3\frac{a_1^4}{r^4} - 2\frac{a_1^2}{r^2}\right)\cos 4\theta\right] \qquad (4)$$

$$\tau_{xy} = -\frac{(\sigma_1+\sigma_2)}{2}\frac{a_1^2}{r^2}\sin 2\theta + \frac{(\sigma_1-\sigma_2)}{2}\left(3\frac{a_1^4}{r^4} - 2\frac{a_1^2}{r^2}\right)\sin 4\theta$$

The stress-strain equations for plane stress are:

$$\epsilon_x = \frac{1}{E}(\sigma_x - \gamma\sigma_y)$$

$$\epsilon_y = \frac{1}{E}(\sigma_y - \gamma\sigma_x) \qquad (5)$$

$$\gamma_{xy} = \frac{1}{G}\tau_{xy}$$

Substituting Equations 4 into Equations 5 one obtains the three strain components ϵ_x, ϵ_y, and γ_{xy} at any point defined by the polar coordinates r and θ. E and γ are Young's modulus and Poisson's ratio, respectively. (See Equation 6)

The difference between the strains given by Equations 6 for the plate with circular hole of radius a_1 and the strains for the solid plate will be computed below. It is to be observed that the strains ϵ_x and ϵ_y are to be measured by the strain gages while the detrusion γ_{xy} shall not be considered.

Consider the uniaxial case with $\sigma_1 = 1$ and $\sigma_2 = 0$ (this is similar to the loading used in the calibration tests in Part III of this paper). For this case Equations 6 reduce to Equation 7:

Let the change in strain in the x-direction due to drilling the hole be $\Delta\epsilon_x = \phi_x$ and the change in the y-direction be $\Delta\epsilon_y = \phi_y$

i.e., $$\Delta\epsilon_x = \phi_x = \epsilon_x^{\text{Hole}} - \epsilon_x^{\text{Solid}}$$

$$\Delta\epsilon_y = \phi_y = \epsilon_y^{\text{Hole}} - \epsilon_y^{\text{Solid}}$$

to be computed as the differences between Equations 6 and 5.

For the solid plate Equations 5 may be written as Equation 5a:

Examination of Equations 8 and 9 show the periodic character of ϕ_x and ϕ_y with maximum at $\theta = 0$ and $\theta = \frac{\pi}{2}$. Because of this periodicity, three values of ϕ may be read in the strain rosette in three arbitrary directions and from these may be found in magnitude and direction the two principal values of ϕ.

Equations 9 may be written as:

$$\phi_x = K_1\epsilon_x + K_2\epsilon_y$$

$$\phi_y = K_1\epsilon_y + K_2\epsilon_x \qquad (10)$$

where ϵ_x and ϵ_y are the strains in the solid plate. The constant K_1 is associated with ϕ_x and ϵ_x or ϕ_y and ϵ_y and the constant K_2 is associated with ϕ_x and ϵ_y or ϕ_y and ϵ_x. These constants are defined for the two principal directions. Solving Equations 10 for K_1 and K_2 we have equation 11.

$$\epsilon_x = \frac{(\sigma_1+\sigma_2)}{2E}\left[1-\nu-(1+\nu)\frac{a_1^2}{r^2}\cos 2\theta\right] + \frac{(\sigma_1-\sigma_2)}{2E}\left[1+\nu-2(1-\nu)\frac{a_1^2}{r^2}\cos 2\theta + (1+\nu)\left(3\frac{a_1^4}{r^4} - 2\frac{a_1^2}{r^2}\right)\cos 4\theta\right]$$

$$\epsilon_y = \frac{(\sigma_1+\sigma_2)}{2E}\left[1-\nu+(1+\nu)\frac{a_1^2}{r^2}\cos 2\theta\right] - \frac{(\sigma_1-\sigma_2)}{2E}\left[1+\nu+2(1-\nu)\frac{a_1^2}{r^2}\cos 2\theta + (1+\nu)\left(3\frac{a_1^4}{r^4} - 2\frac{a_1^2}{r^2}\right)\cos 4\theta\right]$$

$$\gamma_{xy} = -\frac{(\sigma_1+\sigma_2)}{2G}\frac{a_1^2}{r^2}\sin 2\theta + \frac{(\sigma_1-\sigma_2)}{2G}\left(3\frac{a_1^4}{r^4} - 2\frac{a_1^2}{r^2}\right)\sin 4\theta \qquad (6)$$

$$\epsilon_x = \frac{1}{E}\left[1 + \frac{1}{2}(\nu-3)\frac{a_1^2}{r^2}\cos 2\theta + (1+\nu)\left(\frac{3}{2}\frac{a_1^4}{r^4} - \frac{a_1^2}{r^2}\right)\cos 4\theta\right]$$

$$\epsilon_y = \frac{1}{E}\left[-\nu + \frac{1}{2}(3\nu-1)\frac{a_1^2}{r^2}\cos 2\theta - (1+\nu)\left(\frac{3}{2}\frac{a_1^4}{r^4} - \frac{a_1^2}{r^2}\right)\cos 4\theta\right] \qquad (7)$$

$$\epsilon_x = \frac{1}{E}(\sigma_1 - \nu\sigma_2) = \frac{(\sigma_1+\sigma_2)}{2E}(1-\nu) + \frac{(\sigma_1-\sigma_2)}{2E}(1+\nu)$$

$$\epsilon_y = \frac{1}{E}(\sigma_2 - \nu\sigma_1) = \frac{(\sigma_1+\sigma_2)}{2E}(1-\nu) - \frac{(\sigma_1-\sigma_2)}{2E}(1+\nu) \qquad (5a)$$

Thus:

$$\phi_x = \frac{(\sigma_1+\sigma_2)}{2E}\left[-(1+\nu)\frac{a_1^2}{r^2}\cos 2\theta\right] + \frac{(\sigma_1-\sigma_2)}{2E}\left[-2(1-\nu)\frac{a_1^2}{r^2}\cos 2\theta + (1+\nu)\left(3\frac{a_1^4}{r^4} - 2\frac{a_1^2}{r^2}\right)\cos 4\theta\right]$$

$$\phi_y = \frac{(\sigma_1+\sigma_2)}{2E}\left[(1+\nu)\frac{a_1^2}{r^2}\cos 2\theta\right] - \frac{(\sigma_1-\sigma_2)}{2E}\left[2(1-\nu)\frac{a_1^2}{r^2}\cos 2\theta + (1+\nu)\left(3\frac{a_1^4}{r^4} - 2\frac{a_1^2}{r^2}\right)\cos 4\theta\right] \qquad (8)$$

For the uniaxial case with $\sigma_1 = 1$ and $\sigma_2 = 0$ Equations 8 become:

$$\phi_x = \frac{1}{E}\left[\frac{1}{2}(\nu-3)\frac{a_1^2}{r^2}\cos 2\theta + (1+\nu)\left(\frac{3}{2}\frac{a_1^4}{r^4} - \frac{a_1^2}{r^2}\right)\cos 4\theta\right]$$

$$\phi_y = \frac{1}{E}\left[\frac{1}{2}(3\nu-1)\frac{a_1^2}{r^2}\cos 2\theta - (1+\nu)\left(\frac{3}{2}\frac{a_1^4}{r^4} - \frac{a_1^2}{r^2}\right)\cos 4\theta\right] \qquad (9)$$

$$K_1 = \frac{\epsilon_y\phi_y - \epsilon_x\phi_x}{\epsilon_y^2 - \epsilon_x^2}$$

$$K_2 = \frac{\epsilon_y\phi_x - \epsilon_x\phi_y}{\epsilon_y^2 - \epsilon_x^2} \qquad (11)$$

In Equations 7 and 9 let:

$$A = (\nu - 5)\frac{a_1^2}{r^2}\cos 2\theta$$

$$B = (1+\nu)\left(\frac{3}{2}\frac{a_1^4}{r^4} - \frac{a_1^2}{r^2}\right)\cos 4\theta$$

$$C = (3\nu - 1)\frac{a_1^2}{r^2}\cos 2\theta$$

these are constant for any one point in the plate. Equations 7 and 9 become:

$$\epsilon_x^{Hole} = \frac{1}{E}\left[1 + \frac{1}{2}A + B\right]$$

$$\epsilon_y^{Hole} = \frac{1}{E}\left[-\nu + \frac{1}{2}C - B\right] \qquad (7a)$$

$$\phi_x = \frac{1}{E}\left[\frac{1}{2}A + B\right]$$

$$\phi_y = \frac{1}{E}\left[\frac{1}{2}C - B\right] \qquad (9a)$$

Substituting Equations 9a and 5 with $\sigma_x = 1$, $\sigma_y = 0$ in Equations 10:

$$\frac{1}{E}\left[\frac{1}{2}A + B\right] = K_1\left(\frac{1}{E}\right) + K_2\left(-\frac{\nu}{E}\right)$$

$$\frac{1}{E}\left[\frac{1}{2}C - B\right] = K_1\left(-\frac{\nu}{E}\right) + K_2\left(\frac{1}{E}\right) \qquad (12)$$

from which:

$$K_1 = \frac{\frac{1}{2}A + B + \nu\left(\frac{1}{2}C - B\right)}{1-\nu^2}$$

$$K_2 = \frac{\frac{1}{2}C - B + \nu\left(\frac{1}{2}A + B\right)}{1-\nu^2} \qquad (13)$$

In the application of the hole method, the coefficients of sensitivity for the strains (K_1 and K_2) could be determined experimentally from Equations 11 which give K_1 and K_2 in terms of the known initial (or predrilling) strains ϵ_x and ϵ_y and in terms of the measured changes in strain ϕ_x and ϕ_y due to the drilling of the hole.

Figures 6 and 7 show $\Delta\epsilon_x = \phi_x$ and $\Delta\epsilon_y = \phi_y$ as functions of the coordinates r and θ and indicate their periodic nature.

Solving Equations 10 for ϵ_x and ϵ_y (the strains in the solid plate):

$$\epsilon_x = \frac{K_1\phi_x - K_2\phi_y}{K_1^2 - K_2^2}$$

$$\epsilon_y = \frac{K_1\phi_y - K_2\phi_x}{K_1^2 - K_2^2} \qquad (14)$$

With K_1, K_2 known and ϕ_x, ϕ_y measured, the strains ϵ_x and ϵ_y may be calculated from Equations 10. The engineer, however, is usually concerned with the stresses rather than the strains. Thus we may use the well-known stress-strain equations for plane stress:

$$\sigma_x = \frac{E}{1-\nu^2}(\epsilon_x + \nu\epsilon_y)$$

$$\sigma_y = \frac{E}{1-\nu^2}(\epsilon_y + \nu\epsilon_x) \qquad (15)$$

From Equations 14 and 15 we derive Equation 16 next page.

These may be simplified by putting:

$$\lambda_1 = \frac{E}{(1-\nu^2)}\frac{(K_1 - \nu K_2)}{(K_1^2 - K_2^2)}$$

$$\lambda_2 = \frac{E}{(1-\nu^2)}\frac{(K_2 - \nu K_1)}{(K_1^2 - K_2^2)} \qquad (17)$$

$$\sigma_x = \frac{E}{(1-\nu^2)(K_1^2-K_2^2)}\left[\phi_x(K_1-\nu K_2)-\phi_y(K_2-\nu K_1)\right]$$

$$\sigma_y = \frac{E}{(1-\nu^2)(K_1^2-K_2^2)}\left[\phi_y(K_1-\nu K_2)-\phi_x(K_2-\nu K_1)\right] \quad (16)$$

Equations 16 become:

$$\sigma_x = \lambda_1 \phi_x - \lambda_2 \phi_y$$

$$\sigma_y = \lambda_1 \phi_y - \lambda_2 \phi_x \quad (18)$$

where λ_1 and λ_2 are the coefficients of sensitivity for stress. Equations 18 are used in the field-type test—λ_1 and λ_2 are experimentally determined coefficients, ϕ_x and ϕ_y are principal strains (found from the strains measured in a strain rosette), and σ_x and σ_y are the unknown principal stresses in the solid plate.

From Equations 18:

$$\lambda_1 = \frac{\sigma_y \phi_y - \sigma_x \phi_x}{\phi_y^2 - \phi_x^2}$$

$$\lambda_2 = \frac{\sigma_y \phi_x - \sigma_x \phi_y}{\phi_y^2 - \phi_x^2} \quad (19)$$

These equations are used in the calibration type test. The principal stress σ_y $\left(\text{type formula } \sigma_y = \frac{P}{a}\right)$ is known, $\sigma_x = 0$ and ϕ_x and ϕ_y are read by means of the strain gages placed in the x and y directions.

The above discussion for the uniaxial case $\sigma_1 = 1$, $\sigma_2 = 0$ may evidently be extended to the general biaxial loading $\sigma_1 \neq 1$, $\sigma_2 \neq 1$ by using the principle of superposition.

Figures 6 and 7 show the values of ϕ_x and ϕ_y for the uniaxial case $\sigma_1 = 0$ and $\sigma_2 = 1$. In Fig. 7, if a point gage could be placed at $r = 1$, $\theta = \frac{\pi}{2}$ it would have a sensitivity of 2 (the maximum stress concentration in the presence of the hole is 3). However, strain gages have finite dimensions and experience dictated the use of an edge distance (distance from edge of hole to edge of gage). Furthermore, while the values of ϕ_y at $\theta = \frac{\pi}{2}$ are larger than those at $\theta = 0$, in the proximity of the hole, the diagram of the former presents a steeper gradient which gives rise to larger probable errors due to differences in gage location and thickness of plate. For these reasons, it was found more desirable to use radial gages which registered the decrease of strain due to the drilling of the hole rather than the concentration on the side of the hole. That is, for reliability the gage Arrangement I is better than Arrangement II.

Table 1 gives the theoretical values of ϕ_y as a function of r for $\theta = 0$ (see Fig. 7), $\nu = 0.28$, $\sigma_1 = 0$ and $\sigma_2 = 1$.

Table 1

r	ϕ_y
1	−0.72,000
1.05	−0.81,497
1.1	−0.87,043
1.2	−0.90,741
1.3	−0.88,988
1.4	−0.84,714
1.5	−0.79,409
2	−0.54,000

Table 3 in Part III shows that the actual distance from the center of the hole to the center of the A-7 gage is $^1/_2$ in. for a final hole size of $^1/_2$ in., i.e., $r = 2$. The theoretical value for ϕ_y from Table 1, for $r = 2$, is 0.54. This shows that the sensitivity of the method is about one-half of the sensitivity of the gages in their usual application. That is, if the error in gage readings is of the order of 10 microinches per inch, the error in the stress in steel may be approximately 300 psi. With the hole method as described above, the error may be double, say 600 psi. This error does not depend on the intensity of the strain.

Equations 19 as developed for the coefficients λ_1 and λ_2 are needed for the experimental calibration tests. However, it should be noted that Kirsch's formulas never apply rigorously to an actual plate. The formulas are valid for plane stress, i.e., for $\sigma_z = \tau_{zx} = \tau_{zy} = 0$. This is not the case if the plate has a finite thickness, for sections of the plate parallel to the faces do not remain plane during and after drilling the hole and since the deformation of these sections is resisted by the surrounding plate, stress components in the z-direction are developed. A thickness effect is therefore to be expected, and the effect should be greater in the zones where

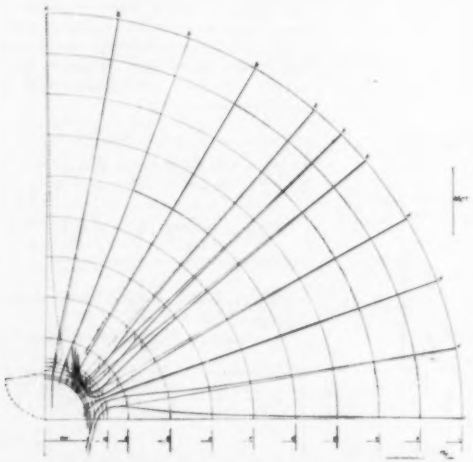

Fig. 6 Plot of equation 8 showing $\Delta\epsilon_x = \phi_x$ as a function of r and θ for uniaxial loading: $\sigma_y = \sigma_2 = 1$ at $r = \infty$, $\sigma_x = \sigma_1 = 0$ at $r = \infty$

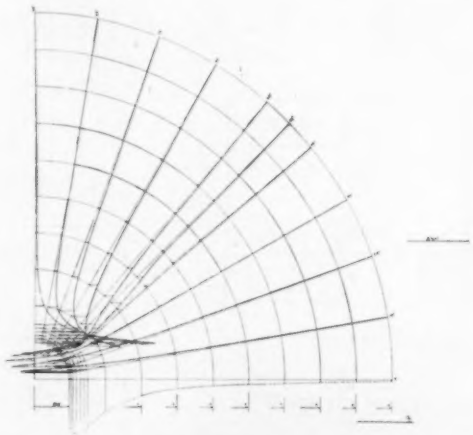

Fig. 7 Plot of equation 8 showing $\Delta\epsilon_y = \phi_y$ as a function of r and θ for uniaxial loading: $\sigma_y = \sigma_2 = 1$ at $r = \infty$, $\sigma_x = \sigma_1 = 0$ at $r = \infty$

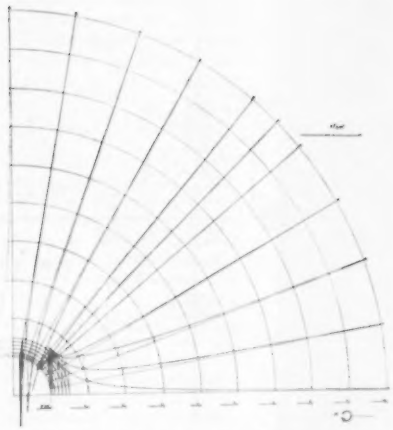

Fig. 8 Plot of $\Delta\epsilon_x$ for unidirectional bending according to Goodier's theory ($\sigma_y = 1$ at $r = \infty$, $\sigma_x = 0$)

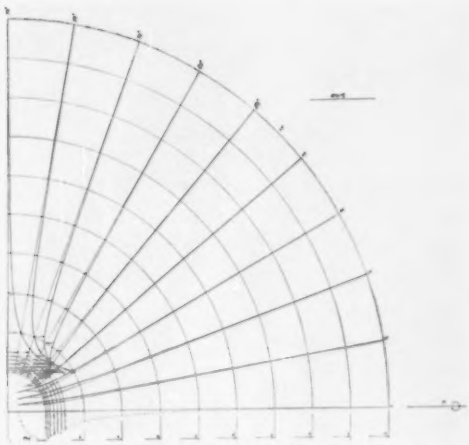

Fig. 9 Plot of $\Delta\epsilon_y$ for unidirectional bending according to Goodier's theory ($\sigma_y = 1$ at $r = \infty$, $\sigma_x = 0$)

the strain gradient is steep. A further limitation exists for very thin plates since the drilling of the hole may cause the plate to buckle in the zones of compression and maximum shear. This behavior is now being analyzed.

THE BENDING PROBLEM

An analysis of the stress distribution in a bent perforated plate is given by Goodier[8] for the case of a thin plate, i.e., the assumption is made that the thickness of the plate is small compared to the diameter of the hole. This is obviously not the practical case. However, it is important to observe that for the bent perforated plate the stress concentration in the neighborhood of the hole is much smaller (compare Figs. 6–9) than in the case of biaxial loading (plane stress) and depends on Poisson's ratio. For steel, Goodier finds a maximum stress at the edge of the hole equal to 1.85 times the stress far away from the hole as compared to 3 for the case of plane stress.

The coefficients λ_1 and λ_2, determined experimentally, for bending of the test plates were found to have values between those for plane stress and those for bending of thin plates.

In the practical application of the hole method it is necessary to take readings on both faces of the plate. The average reading of the strains on the two faces is assumed to be the strain in the middle plane of the plate and from it the stress is computed using the coefficients for plane stress. The difference of the strains (divided by two) on the two faces of the plate is the strain due to bending and the coefficients of bending are then used to compute the additional stress due to bending of the plate.

The analysis of bending, of course, implies the solution of torsion of the plate, which can be reduced to equal and opposite bending moments.

Part III. Experimental Techniques and Test Results

By Edward R. Ward

INTRODUCTION

THE coefficients of sensitivity for stress, λ_1 and λ_2, were determined for tension, compression, and bending in a series of tests performed at Princeton University. Part III covers testing procedures and the results of one hole size for the tension tests only. From the few compressive tests made, the results gave no reason to suspect that the λ values obtained from tension tests are not also applicable to the determination of compressive stresses. The experimental data and theory show that for bending, the λ values vary with plate thickness and further investigation to determine this relationship is necessary. The λ values for tension given did not depend on plate thickness for the sizes tested.

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MATERIALS, EQUIPMENT AND PREPARATION OF TEST PLATES

Test Specimens

With the exception of a few preliminary tests, all the testing was performed on Type 4885 steel supplied by the Navy Department and designated as "project steel." Four sizes of plates were used in the tension tests: 6 x 36 x 3/8 in., 6 x 36 x 1/4 in., 12 x 72 x 3/8 in. and 12 x 72 x 1/4 in.

Gages

Strains were measured with electric resistivity SR-4 strain gages. From experience obtained under a previous contract, it was decided that the Type A-7 gage with 7/64 in. width and 1/4 in. length gave the best compromise between minimum area desired and practical field application. The A-7 gage was therefore used for all calibration tests.

The plates were carefully sanded in the

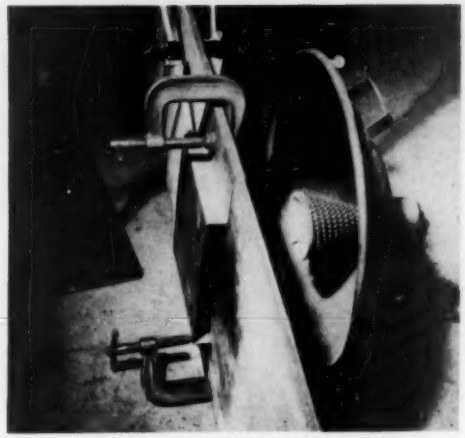

Fig. 10 Method used in baking gages

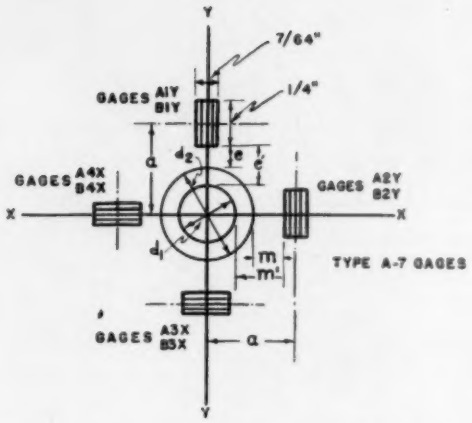

Fig. 11 Gage location for calibration tests, first series

vicinity of the gage location with a power hand sander to remove rust, mill scale and roughness. They were then smoothed with a fine emery cloth and finally cleaned with acetone. After the plates had been prepared, the gages were cemented with SR-4 cement at predetermined locations on opposite sides of the plates. A light pressure was applied to the gages with the finger until the cement had set, after which the pressure was applied with a 1-lb. weight. To avoid different temperature resistance coefficients of the gages, the gages for any one circuit were of the same lot number, resistance, and gage factor. After cementing the gages to the plates, they were allowed to air dry at room temperature for a minimum of 2 hr. The plates were then heated in the vicinity of the gages by means of a 500-watt cone heater, Fig. 10, and the temperature of the plate was raised from room temperature to 70–75° C. in a minimum of 3 hr. This temperature was maintained for at least 12 hr. with an average time of 15 hr.

The temperature in the vicinity of the gages was measured by thermometers on both sides of the plate. Thin sheet-metal shields, shown in Fig. 10, were clamped over the gages on both sides of the plate in order to obtain a more uniform temperature over the heated area and to avoid measuring the radiant heat or the ambient air temperature. The portions of the thermometers outside the shields were protected from the direct rays of the heater by pieces of asbestos (not shown). The temperature was controlled by moving the heater toward or away from the plate.

While the plates were still hot, the gages were covered with a thin coat of Cerese wax, obtained from Baldwin-Southwark. This wax was used to nullify the effect of any change of humidity on the gage readings.

Location of Gages (1st Series—Tests 2–51). Since the λ_1 and λ_2 are obtained from the strain differences in the two principal directions, the gages were placed on the x and y axes of the plates as indicated in Fig. 11. The load in all tests was applied in the y-direction. The arrangement of the gages for this series of tests permitted the determination of three independent sets of λ values. By using the strains obtained for the three combinations of gages shown in Table 2, the coefficients for arrangements I, II and III were obtained. The compensating gages for arrangements I and II were dummy gages cemented to an unstressed plate of the same material.

Location of Gages (2nd Series—Tests 54–79). Results of the first series of tests showed that arrangement I gave the most consistent results. The gages in the second series of tests were therefore arranged radially to give a double arrangement I, as in Fig. 12. In this arrangement, gages 180° apart were read independently and the strains averaged.

Edge Distance

It was desired to hold the edge distance e, Fig. 11, to a minimum so that the sensitivity of the gages around the hole be as large as possible. An edge distance of $^1/_{16}$ in. was first tried for all hole diameters used. This was found insufficient for the $^1/_2$- and 1-in. holes as the gages were sometimes damaged due to the drilling operations and an edge distance of $^1/_8$ in. was standardized for these two hole sizes as the more practical for field use. The $^1/_{16}$-in. edge distance was found sufficient, and retained, for the $^1/_4$- and $^1/_8$-in. holes. A few tests were also made on the $^1/_2$-in. hole with $^3/_{16}$- and $^1/_4$-in. edge distances.

Instrumentation

The strains measured by the strain gages were read on a Baldwin-Southwark Type K electronic strain indicator. This was connected to the gages with No. 18–2 wire SJ cords through a gage circuit-selector switchboard. The switches on this switchboard were of the knife-blade type in order to reduce contact resistance, and they were so wired that any gage could be electrically connected to the indicator as either the measuring or compensating gage. Wiring of one test specimen while another was being tested was accomplished by the use of terminal barrier strips. To these strips were connected No. 18 flexible wires soldered to the strain-gage lead wires. When it was desired to test a plate, it was only necessary to screw the strain gage ends of the SJ cord to these terminal strips. This type of connection was found very satisfactory. One additional precaution was taken to insure against damage to the strain gages from any accidental pull on the lead wire. This

Table 2—Gage Circuit Arrangements

	I		II		III	
Arrangement	λ_1	λ_2	λ_1	λ_2	λ_1	λ_2
Active gage	A1*Y*	A4*X*	A2*Y*	A3*X*	A1*Y*	A4*X*
Compensating gage	Dummy	Dummy	Dummy	Dummy	A2*Y*	A3*X*

B side similar to A side shown above.

consisted of a small plastic block, cemented to the test specimen, to which was attached the soldered connection of the strain-gage leads and the No. 18 wire, Fig. 13.

Method of Loading

Load was applied to the tensile specimens by means of a 200,000-lb. Olsen Universal testing machine. The load was distributed uniformly across the plate by linkages attached to the top and bottom as shown in Fig. 3, Part I. The maximum design load for the linkage was 55,000 lb.

Hole Diameters and Drilling Method

Final hole diameters, d_2 in Fig. 11, of $^1/_8$, $^1/_4$, $^1/_2$ and 1 in. were drilled with high-speed twist drills. In addition, $^1/_2$-in. diameter holes were also formed by drilling a series of twelve overlapping $^1/_8$-in. holes, Fig. 13. A few $^1/_2$-in. diameter holes were drilled with a $^1/_2$-in. counterbore having a $^1/_8$-in. pilot, and one 1-in. hole was drilled with a 1-in. counterbore having a $^3/_8$-in. pilot.

Guide holes, d_1 in Fig. 11, were drilled with either a No. 57 (0.043 in.) or a $^1/_8$-in. high-speed twist drill. The smaller size was used with the $^1/_8$-in. final hole and the larger with all other hole sizes.

Care was taken to keep the drills sharp and the drilling was done at a rate that would not heat the gages and affect their readings. In order to drill the holes normal to the plate, guides for the various drill sizes were designed to clamp to the plates.

CALIBRATION TEST PROCEDURE

The standard procedure for conducting tensile calibration tests was as follows:

1. No hole condition (i.e., solid plate). The loading sequence was: zero load, approximately $^1/_4$, $^1/_2$, $^3/_4$ and maximum load. The maximum load for all specimens was either 55,000 lb. or the load equivalent to a 15,000-psi. stress in the plate, whichever was the lower.
2. Guide hole. The guide holes in all tests were drilled at maximum load.
3. Guide-hole condition. The loading sequence was the same as for the no-hole condition.
4. Final hole. This hole was also drilled at maximum load.
5. Final-hole condition. The loading sequence was the reverse of the no-hole and the guide-hole conditions.

Strain readings were taken for the gages on both sides of the plate for each hole condition and load. All readings were taken in so-called normal and reverse positions by using a reversing switch on the switchboard to interchange the active and compensating gages. These two readings were taken one immediately after the other for each gage circuit. In addition to read-

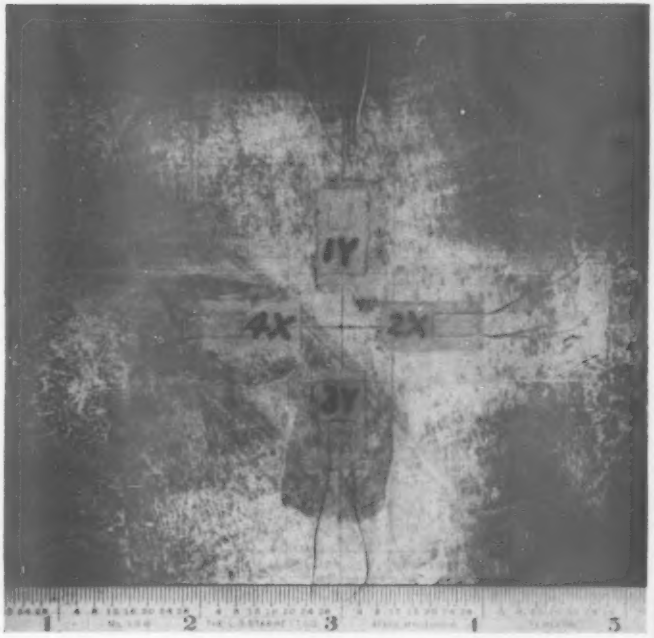

Fig. 12 Gage arrangement for calibration tests, second series

Fig. 13 Calibration test using twelve overlapping $^1/_8$-in. holes

Table 3

λ_1 and λ_2 values

FIRST SERIES

ARRANGEMENT I (No Hole to Final Hole)
Final Hole Diameter = 1/2"

	Test Number	Nominal Plate Dimensions (inches)	Tension λ_1 P.S.I.	Tension λ_2 P.S.I.	Bending λ_1 P.S.I.	Bending λ_2 P.S.I.
		d_2 = 0.500" a = 0.437" e = 0.063"				
	4	1/2 x 6 x 36	- 64.4	+ 25.7	- 127.4	+ 51.9
(a)	8	1¼ x 6 x 12	- 59.6	+ 19.0		
		d_2 = 0.500" a = 0.500" e = 0.125"				
	26	3/8 x 6 x 36	- 58.9	+ 22.4	- 78.0	- 0.37
	27	"	- 62.5	+ 24.5	- 78.1	+ 3.12
	36	"	- 56.3	+ 20.8	- 81.0	- 2.37
	38	"	- 62.5	+ 25.1		
	39	"	- 64.4	+ 28.2		
	45	"	- 60.9	+ 24.8		
	47	3/8 x 12 x 72	- 71.4	+ 30.3		
	48	"	- 61.1	+ 23.6		
(b)	51	"	- 66.4	+ 23.7		
	33	3/4 x 6 x 36	- 55.4	+ 19.3		
	34	"	- 56.7	+ 19.2	- 64.0	+ 12.45
	41	"	- 59.5	+ 23.2		
	42	"	- 72.6	+ 32.9		
	43	"	- 62.9	+ 23.2		
	44	"	- 65.5	+ 25.3		
	49	3/4 x 12 x 72	- 60.6	+ 20.6		
(a)	13	1¼ x 6 x 12	- 68.4	+ 27.4		
(a)	16	"	- 65.5	+ 24.7		

(a) Compression
(b) Twelve 1/8" holes
All holes drilled with 1/2" twist drill except test 51.

λ_1 and λ_2 values

SECOND SERIES

ARRANGEMENT I (No Hole to Final Hole)
Final Hole Diameter = 1/2"

Test Number	Method of Drilling Hole	Nominal Plate Dimensions (inches)	Tension λ_1 P.S.I.	Tension λ_2 P.S.I.
		d_2 = 0.500" a = 0.500" e = 0.125"		
76	1/2" Twist Drill	3/8 x 6 x 36	- 62.2	+ 25.5
58	12 - 1/8" holes	3/8 x 6 x 36	- 56.6	+ 26.4
59	"	"	- 62.4	+ 24.7
63	"	"	- 53.6	+ 20.6
69	"	"	- 60.8	+ 24.0
70	"	"	- 63.4	+ 26.3
71	"	"	- 66.1	+ 25.0
72	"	"	- 67.1	+ 29.5
73	"	"	- 66.7	+ 23.2
74	"	"	- 60.5	+ 24.1
75	"	"	- 62.0	+ 25.3
77	"	"	- 62.9	+ 26.2
78	"	"	- 59.0	+ 20.8
79	"	"	- 60.4	+ 25.1
62	1/2" Counterbore	3/8 x 6 x 36	- 57.4	+ 21.8
68	"	"	- 58.3	+ 21.6
		d_2 = 0.500" a = 0.563" e = 0.188"		
64	12 - 1/8 holes	3/8 x 6 x 36	- 80.5	+ 36.3
66	"	"	- 78.5	+ 32.1
54	1/2" Counterbore	3/8 x 6 x 36	- 72.5	+ 30.5
55	"	"	- 75.2	+ 32.8
		d_2 = 0.500" a = 0.625" e = 0.250"		
66	12 - 1/8 holes	3/8 x 6 x 36	- 91.6	+ 38.0
67	"	"	- 98.8	+ 46.7
60	1/2" Counterbore	3/8 x 6 x 36	- 90.6	+ 38.6
61	"	"	- 96.1	+ 42.3

Table 4

λ_1 and λ_2 values

FIRST SERIES

ARRANGEMENT I (Guide Hole to Final Hole)
Final Hole Diameter = 1/2"

	Test Number	Nominal Plate Dimensions (inches)	Tension λ_1 P.S.I.	Tension λ_2 P.S.I.	Bending λ_1 P.S.I.	Bending λ_2 P.S.I.
		d_1 = 0.125" d_2 = 0.500" a = 0.437" e = 0.063"				
	4	1/2 x 6 x 36	- 60.6	+ 18.5	- 76.6	+ 3.1
(a)	9	1¼ x 6 x 12	- 62.9	+ 20.6		
		d_1 = 0.125" d_2 = 0.500" a = 0.500" e = 0.125"				
	26	3/8 x 6 x 36	- 62.5	+ 25.2	- 82.5	+ 0.37
	27	"	- 69.6	+ 29.2	- 97.5	0
	36	"	- 62.6	+ 23.2	- 90.9	- 0.94
	38	"	- 67.4	+ 26.6		
	39	"	- 69.4	+ 27.0		
	45	"	- 66.5	+ 25.0		
	47	3/8 x 12 x 72	- 73.5	+ 30.7		
	48	"	- 64.7	+ 24.2		
(b)	51	"	- 67.8	+ 25.7		
	33	3/4 x 6 x 36	- 61.6	+ 21.9		
	34	"	- 59.6	+ 20.8	- 70.5	+ 1.19
	41	"	- 59.2	+ 21.0		
	42	"	- 71.0	+ 27.0		
	43	"	- 66.6	+ 25.2		
	44	"	- 64.4	+ 22.1		
	49	3/4 x 12 x 72	- 67.7	+ 27.1		
(a)	13	1¼ x 6 x 12	- 71.1	+ 27.4		
(a)	16	"	- 69.5	+ 25.6		

(a) Compression
(b) Twelve 1/8" holes
All holes drilled with 1/2" twist drill except test 51.

λ_1 and λ_2 values

SECOND SERIES

ARRANGEMENT I (Guide Hole to Final Hole)
Final Hole Diameter = 1/2"

Test Number	Method of Drilling Hole	Nominal Plate Dimensions (inches)	Tension λ_1 P.S.I.	Tension λ_2 P.S.I.
		d_1 = 0.125" d_2 = 0.500" a = 0.500" e = 0.125"		
76	1/2" Twist Drill	3/8 x 6 x 36	- 67.8	+ 28.1
58	12 - 1/8" holes	3/8 x 6 x 36	- 73.1	+ 29.5
59	"	"	- 66.7	+ 24.7
63	"	"	- 66.0	+ 25.2
69	"	"	- 62.9	+ 24.6
70	"	"	- 68.4	+ 27.4
71	"	"	- 67.8	+ 24.1
72	"	"	- 73.8	+ 33.0
73	"	"	- 70.8	+ 29.1
75	"	"	- 64.8	+ 24.8
77	"	"	- 66.5	+ 26.1
78	"	"	- 63.7	+ 22.7
79	"	"	- 65.3	+ 25.0
62	1/2" Counterbore	3/8 x 6 x 36	- 68.9	+ 19.7
68	"	"	- 62.8	+ 23.3
		d_1 = 0.125" d_2 = 0.500" a = 0.563" e = 0.188"		
64	12 - 1/8" holes	3/8 x 6 x 36	- 82.8	+ 35.3
65	"	"	- 79.4	+ 32.6
54	1/2" Counterbore	3/8 x 6 x 36	- 76.9	+ 30.1
55	"	"	- 80.1	+ 34.8
		d_1 = 0.125" d_2 = 0.500" a = 0.625" e = 0.250"		
66	12 - 1/8" holes	3/8 x 6 x 36	- 99.0	+ 40.3
67	"	"	-104.9	+ 47.7
60	1/2" Counterbore	3/8 x 6 x 36	- 93.4	+ 38.9
61	"	"	-100.0	+ 42.7

ing each active gage separately against a dummy compensating gage, the gage on one side of the plate was used as the active gage and the corresponding gage on the opposite side was used as the compensating gage.

As soon as the above readings were taken, the normal and reverse readings for each of the gage circuits were added. The sum of the normal and reverse readings for any one gage circuit remains constant for any load condition except when there is a zero instrument shift, in which case the sum differs by twice the shift. A simple method of spotting erroneous readings inherent in the taking of a large number of recordings was to compare these sums for different loadings.[8] If these sums did not agree, readings were repeated immediately and the error was corrected before any change in loading condition of the plate.

COMPUTATION TECHNIQUES

The strain gage readings for the various gage circuits at zero load for each of the three-hole conditions (including the no-hole condition) were used as the initial readings. These readings were subtracted from the gage-circuit readings at the various loads for the same hole condition. In all cases, for both normal and reverse readings, the strains were obtained in micro-inches per inch. By averaging these two strain readings, the zero instrument shift was eliminated.[6,7] Correction for any accidental bending strains in the tensile calibration tests was made by using strain data from corresponding gages on both sides of the plates.

For each hole condition and for each gage circuit, the strains for both sides of the plate were plotted against the loads and an average line drawn through the plotted points. The strains for an arbitrary load increment were read from the curves and tabulated for each hole condition and gage circuit.

Differences in the strains between any two-hole conditions were called ϕ_x and ϕ_y depending on the orientation of the gage. The values of λ_1 and λ_2 for arrangements I, II or III for the cases: no hole to guide hole, guide hole to final hole and no hole to final hole were computed from the Equations 19, Part II in which $\sigma_z = 0$:

$$\lambda_1 = \sigma_y \frac{\phi_y}{\phi_y^2 - \phi_x^2}$$

$$\lambda_2 = \sigma_y \frac{\phi_x}{\phi_y^2 - \phi_x^2}$$

RESULTS OF CALIBRATION TESTS

The tension coefficients of sensitivity for stress, λ_1 and λ_2, for arrangement I, no hole to final $^1/_8$-in. hole, are given in Table 3 for the first and second series of tests. Corresponding values of λ_1 and λ_2 for the guide hole to final-hole condition are given in Table 4. The tables for the first series of tests also give the few values of λ_1 and λ_2 obtained for bending although these are not discussed here. Average λ_1 and λ_2 values for various plate thicknesses, methods of drilling and edge distances are given in Table 5.

The stresses in the solid plate at the maximum load in a given test specimen were computed by the hole method using the λ_1 and λ_2 values, obtained experimentally for the particular specimen, in the equations:

$$\sigma_x = \lambda_1\phi_x - \lambda_2\phi_y$$
$$\sigma_y = \lambda_1\phi_y - \lambda_2\phi_x$$

These stresses σ_x and σ_y were compared with the stresses as computed from the known load and cross-sectional area. With a few exceptions, it may be observed in Table 6 that the differences in the stresses given under columns headed uncor. $\Delta\sigma_y$ and uncor. $\Delta\sigma_x$ vary from 2000 to 12,000 psi. These were the stresses present in the plates[8] before the test load was applied. As shown later, these values of the initial stresses are of the same order of magnitude for the various plates stress relieved at the same time and in the same manner. As indicated in Part I, the stresses obtained by the use of the hole method are the *sum* of the initial stresses plus any other stresses caused by external loads or body forces.

Table 5

AVERAGE λ_1 AND λ_2 VALUES FOR TENSION

			No Hole to Final Hole			Guide Hole to Final Hole		
	Method of Drilling	Plate Thickness	Number of Tests	λ_1 p.s.i.	λ_2 p.s.i.	Number of Tests	λ_1 p.s.i.	λ_2 p.s.i.
	for Edge Distance e = 1/16"							
(a)	1/2" Twist Drill	$\frac{1}{2}$" and $1\frac{1}{4}$"	2	- 61.6	+ 22.4	2	- 61.3	+ 19.6
	for Edge Distance e = 1/8"							
(b)	1/2" Twist Drill	5/8"	9	- 62.2	+ 24.3	9	- 67.0	+ 25.2
	"	3/4"	7	- 61.7	+ 25.4	7	- 64.3	+ 23.6
	"	1 1/4"	2	- 67.0	+ 26.1	2	- 70.5	+ 26.5
	"	5 Thicknesses	15	- 62.5	+ 24.4	18	- 66.3	+ 25.2
	12 - 1/8" holes	5/8"	14	- 63.1	+ 25.2	13	- 67.5	+ 26.3
	1/2" Counterbore	"	2	- 59.1	+ 21.7	2	- 60.9	+ 21.5
	All Tests		34	- 62.5	+ 24.6	33	- 66.4	+ 25.4
	for Edge Distance e = 5/16"							
	12 - 1/8" holes	5/8"	2	- 78.0	+ 34.2	2	- 81.1	+ 34.0
	1/2" Counterbore	"	2	- 74.4	+ 31.7	2	- 78.5	+ 32.5
	All Tests		4	- 76.2	+ 33.0	4	- 79.3	+ 33.3
	for Edge Distance e = 1/4"							
	12 - 1/8" holes	5/8"	2	- 96.7	+ 42.4	2	-102.0	+ 44.0
	1/2" Counterbore	"	2	- 92.8	+ 40.5	2	- 96.7	+ 40.8
	All Tests		4	- 94.8	+ 41.6	4	- 99.4	+ 42.4

(a) One tension and one compression test
(b) Compression tests

Table 6

DIFFERENCES BETWEEN COMPUTED AND APPLIED STRESSES PSI.

Test Number	uncor. $\Delta\sigma_y$	uncor. $\Delta\sigma_x$	cor. $\Delta\sigma_y$	cor. $\Delta\sigma_x$	Test Number	uncor. $\Delta\sigma_y$	uncor. $\Delta\sigma_x$	cor. $\Delta\sigma_y$	cor. $\Delta\sigma_x$
for Edge Distance e = 1/16"									
4	+ 7596	+ 9073	+ 5092	+ 3435	8	+ 1587	- 6571	- 490	- 448
for Edge Distance e = 1/8"									
13	+ 6731	+ 9994	- 158	- 551	51	+ 5780	+ 1779	+ 212	+ 296
16	+ 4332	+ 4586	+ 101	+ 519	59	+ 2350	+ 1614	- 10	- 65
20	+ 5999	+ 8617	+ 193	+ 158	59	+ 2411	+ 1456	+ 168	+ 25
27	+ 6515	+ 9350	+ 150	- 224	62	+ 5762	+ 2254	+ 452	+ 339
35	+ 9440	+ 8705	- 270	+ 56	63	+ 1929	+ 116	- 433	- 614
54	+ 6094	+ 9950	- 690	- 551	68	+ 2472	+ 656	+ 169	+ 42
36	+ 6961	+ 5889	+ 48	+ 183	69	+ 9474	- 1995	+ 182	- 166
38	+ 5375	+ 2935	- 250	- 96	70	+ 7189	+ 2212	+ 147	+ 356
39	+ 3851	+ 2215	+ 83	- 179	71	+ 5857	- 498	+ 92	- 474
41	+ 5941	+ 4446	+ 98	+ 45	72	+ 2065	+ 1194	+ 399	+ 142
42	+ 9325	+ 7605	+ 127	+ 563	73	+ 3458	+ 1229	+ 189	- 152
43	+ 6498	+ 6037	- 94	- 289	74	- 854	+ 1463	+ 820	- 212
44	+11531	+ 9406	+ 39	+ 236	75	+ 2554	+ 1198	- 175	- 176
46	+ 7439	+ 4976	- 398	- 895	76	+11989	+ 8965	+ 519	+ 222
47	+ 5823	+ 4207	+ 1055	+ 697	77	+ 2657	+ 953	+ 273	- 41
48	+ 6510	+ 5427	+ 576	+ 43	78	+ 2457	0	+ 94	- 579
49	+ 6069	+ 4404	- 394	- 259	79	+ 2750	- 1526	+ 406	- 40
for Edge Distance e = 5/16"									
54	+ 4394	+ 1970	+ 396	+ 249	64	+ 2298	+ 856	+ 108	+ 125
55	+ 2108	+ 1346	- 29	- 121	65	+ 1450	+ 145	- 127	- 215
for Edge Distance e = 1/4"									
60	+ 5117	+ 709	- 67	- 502	66	+ 2500	+ 758	+ 582	+ 275
61	+ 2253	+ 185	+ 754	+ 565	67	+ 3826	+ 1649	+ 625	+ 401

Strain differences, due to the initial stresses in the test plates, existed among the three-hole conditions at zero load. Using these differences with the opposite sign as corrections for the strain differences at maximum load, the stresses were recomputed. Comparison was again made with the stresses calculated from the known load. The differences as listed in Table 6 as cor. $\Delta\sigma_x$ and cor. $\Delta\sigma_y$ show agreement within ±500 psi. in 75% of the tests and within ±1000 psi. in all tests (except for test 4 in which the edge distance was insufficient.)

FIELD TYPE TEST

A simulated field test 37 was performed on a $^3/_8$ x 6 x 36-in. stress-relieved project steel plate. Type A-7 gages were placed in the form of a rosette, shown in Fig. 14, with the center of each gage $^1/_2$ in. from the intersection of the x and y axes. Readings were taken for the no-hole condition at zero load and at 35,000-lb. tensile load. While the plate was still under this load a $^1/_2$-in. hole was drilled in the center of the rosette. Readings were again taken at both maximum and zero loads.

A dyadic strain circle was drawn using the strain differences for gages 1, 2 and 3 between the no-hole and $^1/_2$-in. hole conditions at maximum load. From this circle were read the principal strain differences ϕ_x' and ϕ_y' and their directions. These values, and the average $\lambda_1 = -62.5$ and $\lambda_2 = +24.4$ from Table 5 for $^1/_2$-in. twist drill and $^1/_2$-in. edge distance were substituted in Equations 18, Part II. Comparison between these computed stresses (called uncor. σ_x' and uncor. σ_y'), the actual applied stresses, and their directions are given in Table 7. Corrections using the strain differences at zero load were made and the stresses recalculated. These corrected stresses (called cor. σ_x' and cor. σ_y') are also given in Table 7.

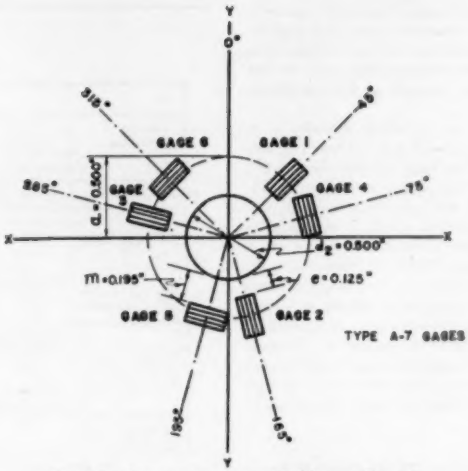

Fig. 14 Gage arrangement field type Test 37

Table 7—Comparison of Computed and Applied Stresses—Test 37

Stress	*Uncorrected*	*Corrected*	*Actual*
	$\theta = 86°45'$	$\theta = 89°$	$\theta = 90°$
σ_x'	−1137 psi.	−502 psi.	0 psi.
σ_y'	−17,753 psi.	−15,842 psi.	−15,500 psi.

The orthogonal gages 4, 5 and 6 would have given the strain differences for arrangement II. From the λ values for this arrangement, the stresses could also have been computed. With the radial gages as the measuring gages and the orthogonal gages as the compensating gages, arrangement III could be obtained.

A second field-type test was made on the same size plate as in Test 37. The six gages on each side of the plate in this test were all mounted radially with their centers $^1/_2$ in. from the intersection of the x and y axes, as in Fig. 1, Part I. The strain differences for gages 180° apart were averaged before the dyadic strain circle was drawn. It is for this reason that the gage arrangement in this test is recommended for field tests.

DISCUSSION

In tests 2–51, the λ values obtained for arrangement II (not given in this paper) were erratic but did show a variation with the plate thickness. Arrangement III showed a similar variation and the λ values were fairly consistent for a given plate thickness. However, these values were considerably smaller than the values for arrangement I. Arrangement I had the additional advantage of permitting a double set of gages whose readings could be averaged and does not appear to depend on plate thickness for the sizes tested.

The tests 54–79 were therefore made with a double-gage arrangement I. The values of the tension coefficients λ_1 and λ_2 for the $^1/_2$-in. holes drilled with the twist drill check closely the values for the $^1/_2$-in. scalloped holes drilled by overlapping twelve $^1/_2$-in. holes, with the values of the $^1/_2$-in. counterbore hole somewhat smaller. The same agreement is evident in the few readings obtained for the tests with $^1/_{16}$ and $^1/_4$ in. rather than the standard $^1/_8$-in. edge distance.

It may be observed in Fig. 15 that for lots 8 and 10 the differences between uncorrected stresses obtained by the hole method and those obtained from known applied loads are in the order of 2500 psi. These differences were also found in the field-type test 37 and are the initial stresses remaining in the plates *after* stress relieving. Such initial stresses remain in a stress-relieved body and they are never less than the proportional limit of the material at the stress-relieving temperature employed.[9] The proportional limit of ordinary carbon steel is about 2500 psi. at 1150° F., the stress relieving temperature used. It is felt that the stress relieving, done locally by a commercial concern, of lots 5, 6 and 7 was not done properly and that the initial stresses were not reduced below the values given in Table 6 as uncor. $\Delta\sigma_x$ and uncor. $\Delta\sigma_y$. The three plates stress relieved by R.C.A. and over which there was close supervision during the relieving process, showed initial stresses remaining in the plates of approximately 2500 psi.

In a field test the guide hole is drilled to permit exact centering of the gages on opposite sides of the plate. This hole along with another reference hole of the same size drilled at least 12 in. away are used to establish a reference axis. If it is possible to orient and locate the gages properly without the use of these holes, they may be omitted and the coefficients λ_1 and λ_2 for the no-hole to final-hole condition may be used.

RECOMMENDED FIELD PROCEDURE

1. Drill $^1/_2$-in. guide hole at the point where the stresses are desired. This hole and all other holes (including the reference hole) should be drilled using a drill guide to insure that the hole is drilled normal to the plate.

2. Remove paint, rust, mill scale,

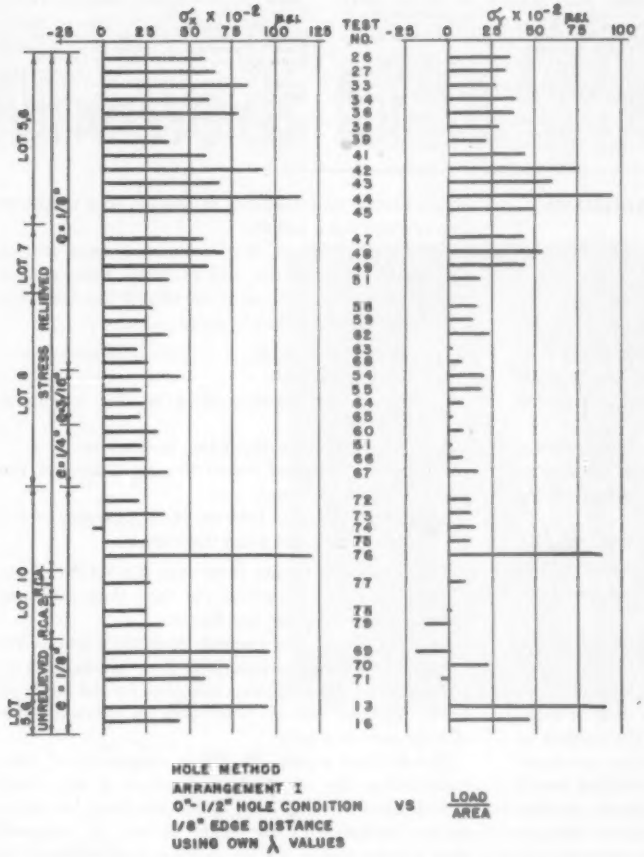

Fig. 15 Difference between computed and applied stresses

grease, etc. on both sides of the plate and smooth the area around the guide hole by careful sanding or grinding. Finish with a fine emery cloth until the surface is smooth and bright.

3. Use the centers of the guide and reference holes to establish a reefrence axis on both sides of the plate.

4. From the reference axis lay out radial lines 60° apart from the center of the guide hole.

5. Scribe a $^3/_4$-in. diameter circle around the center of the guide hole.

6. Clean the plate with acetone.

7. Cement one type A-7 strain gage centered along each of the six radial lines on each side of the plate with the bottom of the wire elements tangent to the $^3/_4$-in. circle.

8. Allow the gages to air dry or bake as recommended by the manufacturer.

9. Wax the gages lightly after moisture has been removed.

10. Solder No. 18 flexible wire to gage leads and attach free ends to terminal barrier strips. Fasten soldered connections to small plastic blocks cemented to the plate so as to absorb any accidental pull on the gage leads.

11. Attach wires from gage selector switchboard to the terminal barrier strips.

12. Take a complete set of readings. Read all gages against the dummy compensating gage of the same lot number, resistance and gage factor. Read also all the gages on the drilling side of the plate against those on the opposite side. This second set of readings gives double the bending strains. Read all gages in both normal and reverse positions.

13. Drill twelve overlapping $^1/_4$-in. holes with their centers $^3/_{16}$ in. from the center of the guide hole. A jig for drilling these holes is desirable.

14. Take a complete set of readings as in 12 above.

15. Subtract the readings for the guide-hole condition from the readings for the final-hole condition. The sign is correct for normal reading, and opposite for reverse readings.

16. Average the strain differences for normal and reverse readings with proper sign.

17. Correct these strain differences by subtracting one-half the bending strain with the proper sign from the gages on the drilling side and adding one-half to the gages on the opposite side.

18. Compare gage circuits 180° apart and on both faces of plate. Discard any apparent erroneous strain. Average the remaining values. This gives the values ϕ_1, ϕ_2 and ϕ_3.

19. From the dyadic strain circle construction, Fig. 2, Part I (or any other preferred procedure) find the principal strains. This gives ϕ_y' and ϕ' and their directions with respect to the reference axis.

20. Substitute ϕ_x' and ϕ_y' from 19 above, and the known coefficients λ_1 and λ_2 for arrangement I, $^1/_8$-in. edge distance, guide hole to final $^1/_4$-in. hole, Table 5, in the equations:

$$\sigma_x' = \lambda_1\phi_x' - \lambda_2\phi_y'$$
$$\sigma_y' = \lambda_1\phi_y' - \lambda_2\phi_x'$$

This gives the principal stresses existing in the plate at the point being investigated.

CONCLUSION

From the tests accomplished, the following conclusions may be drawn:

1. The hole method may be used to determine stresses with a probable accuracy of 500 to 1000 psi. In the tensile tests performed, 75% of the tests checked within 500 psi. and all tests checked within 1000 psi.

2. The preferred gage arrangement is the double arrangement I rosette (6 gages at 60° to one another) Fig. 1, Part I.

3. In most cases, the $^1/_4$-in. final hole with $^1/_8$-in. edge distance should be used.

4. The best results are to be expected by drilling a scalloped $^1/_2$-in. hole by overlapping twelve $^1/_4$-in. holes. A $^1/_4$-in. high-speed twist drill may be used instead but great care should be taken not to heat the gages by the drilling operation or to damage the gages with chips from the hole.

5. More work should be done on the determination of coefficients for smaller final-hole sizes such as $^1/_4$ and $^1/_8$ in. to be used where it would be undesirable to use a $^1/_2$-in. hole.

6. More work should be done utilizing the hole method to determine the values of the initial stresses in unloaded hot rolled steel plates.

7. More work should be done on the determination of coefficients for bending as the tension coefficients are not applicable in this case.[9] Investigation should

be carried out to show how the bending coefficients vary with plate thickness.

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International Welding Commission

(*Continued from page 90-s*)

to brittle fracture according to the dimensions of a construction.

4. It should be noticed that it is difficult to obtain sufficiently precise information on concrete cases of brittle fracture. Commission 12, therefore, appeals to all its members to try to obtain the largest number of possible experimental facts.

5. Several of the documents at the disposal of the Commission mentioned the influence of all the factors concerned with the conception and execution of welded work.

Commission 12 has particularly noted the importance of not losing sight of this essential aspect of the question which was also discussed during the joint meeting of Commissions 10, 11 and 12.

This serves to remind us of the complexity of the problem—a complexity largely caused by the great number of factors which must be considered in their order of importance. It should also be kept in mind that this order may vary according to the method of construction in mind. In this connection care must be taken not to lay down too rigid criteria which would run the risk of completely paralyzing one or another branch of welded construction. Therefore when a certain number of precise criteria have been arrived at, they must be carefully examined, bearing in mind the conditions imposed by the nature of the various branches of construction.

Commission 12, therefore, thinks that the problem should be prudently tackled by seeking to analyze the influence of the factors successively and entirely objectively. For this reason, during the present session, the Commission voluntarily restricted its studies to the question for which it was best prepared; it has, therefore, tackled the task of discovering a test allowing of the compilation of a first classification of steels from the point of view of their susceptibility to brittle fracture.

In the course of a discussion directed by Mr. Herbiet for Sub-Commission "a" the Commission was in complete agreement in deciding to take into account, in order to reduce the number of tests, the following three factors:

1. Study of stresses.
2. Speed of deformation.
3. Temperature.

The various types of steel which we must try to obtain should be considered in relation to a combination of these three factors.

All the members of the Commission were also in agreement regarding the way in which these studies should be carried out so as to obtain the following experimental information:

1. Maximum strength.
2. Fracture strength.
3. Deformation corresponding to the maximum strength.
4. Deformation corresponding to fracture.
5. Energy absorbed before the appearance of the maximum stress.
6. Energy absorbed between the appearance of the maximum stress and the fracture.

These two last results come into the sphere of resilience. But we underline the fact that resilience measurements alone are insufficient.

In the light of this research plan the Commission proceeded to the examination of the various tests at present used in the different countries for the study of brittle fractures and has underlined the characteristics of these tests in a table.

In a brilliant report, Mr. Bihet, rapporteur of Sub-Commission 12b, clarified the position of new theoretical researches which are at present being, or about to be, undertaken. He made known the research plan jointly drawn up by Belgium and Holland to throw light, if possible, on the researches with which Sub-Commission 12a is concerned. He pointed out in particular that this plan envisaged a study of the common ground between the fields of elastic deformation governed by Hook's law and that of plasticity. It was made clear by common agreement that these researches would not be theoretical but experimental so as to serve as a connecting link between physics properly speaking and the study of brittle fractures by Sub-Commission 12a.

I should not complete my duty if I did not congratulate the members of our Commission and in particular the rapporteurs and secretaries on their team spirit and if I did not thank them for the great energy with which they have worked.

To conclude, I should mention that the Governing Council of the International Institute asked us to nominate a permanent European Vice-Chairman responsible for the liaison with Dr. Finn Jonassen of the United States, recently appointed Chairman of Commission No. 12. Our unanimous choice has fallen upon Dr. Haringx who so ably presided over our meetings at Delft.

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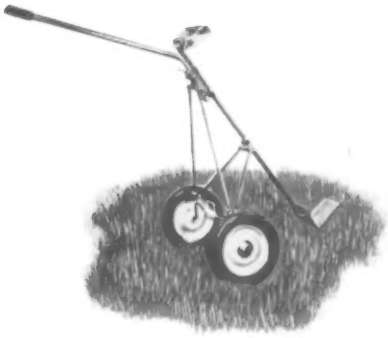

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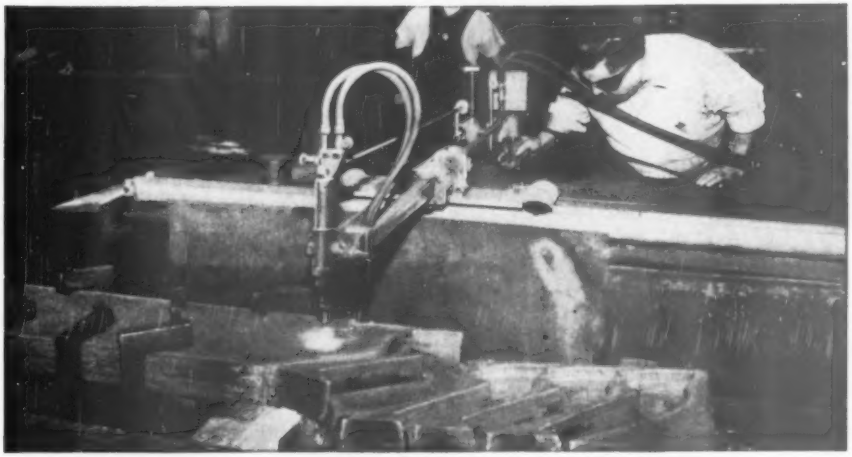

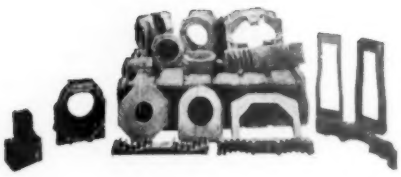

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